Thermal-hydraulic analysis of liquid metal pool experiment
E-SCAPE

RELATORI

Prof. Ing. Francesco Oriolo

Prof. Ing. Walter Ambrosini

Dott. Ing. Nicola Forgione

Ing. Katrien Van Tichelen (SCK · CEN)

Ing. Diego Castelliti (SCK · CEN)

IL CANDIDATO

Alessandro Marino

Anno Accademico 2010/2011
Abstract

In this work the design of the liquid metal pool experiment E-SCAPE has been supported by the use of similarity theories and system code calculations. The E-SCAPE facility is a thermal-hydraulic scaled model of the MYRRHA reactor and will address key phenomena relevant for liquid metal cooled reactors.

In the first part of the work a scaling analysis was performed for normal operating conditions using different scaling approaches. From the first scaling analysis results it was decided to adopt the Froude number preservation criterion for scaling velocities, and a length scaling factor of 1/6.3 to scale the MYRRHA dimensions in order to obtain a facility which represents a small version of the actual plant (isotropic scaling in all the dimensions).

A RELAP 5 system code model of the facility, in its first design configuration, has been developed to validate the Froude number conservation approach and to obtain a preliminary thermo-hydraulic characterization of E-SCAPE during operational and accidental transients. The simulations were performed using a version of the RELAP5/Mod.3.3 code purposely modified to account for LBE properties and behavior. The obtained results for the considered transients allowed to assess in detail whether the current plant layout can accommodate these transients, and confirmed the validity of the Froude number preservation scaling approach. Consequently a first MYRRHA thermal-hydraulic characterization for normal operating conditions was obtained scaling up the main parameters analyzed.

In the second part of the work it has been considered the possibility to predict using E-SCAPE the MYRRHA natural circulation phenomena, keeping the main design already available for forced convection flow. For this purpose it was used a scaling approach which would preserve both the Richardson and the Froude numbers. Contrary to the case of forced convection, a detailed analysis of the natural circulation transient for the most recent geometry was not available at this stage of the project. Consequently a simplified steady state analytic solution of the reactor natural circulation capability has been developed. Starting from this solution we obtained, with the scaling considerations, a very first prediction of E-SCAPE behavior in natural circulation which was checked with RELAP 5 simulations on an ideal scaled version of the facility with the heat exchanger located inside the vessel.

A good agreement between the value of natural circulation flow rate expected from the scaling analysis and the system code results was found. Consequently it seemed possible to obtain an integrated test facility for both forced and natural circulation, just changing the position of the heat exchangers from the first design developed for forced convection flow.
Acknowledgements

Vorrei ringraziare in primis il Prof. Oriolo, una cui e-mail è stata sufficiente a permettermi di portare avanti questa esperienza stupenda e che sempre ricorderò. Un sentito ringraziamento a tutti coloro chi mi ha seguito in questo lavoro, per la continua disponibilità e pazienza nei miei confronti.

A special thanks to SCK·CEN, and in particular to Katrien Van Tichelen.
It was a special experience that I will never forget.
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List of abbreviations and symbols

ADS   Accelerator driven system
BR1   Belgian reactor 1
BR2   Belgian reactor 2
BR3   Belgian reactor 3
CERN  Conseil Europeen pour la recherche nucleaire
CFD   Computational fluid dynamics
CRIEPI Central Research Institute of Electric Power Industry
ELSY  European Lead-cooled System
EOS   Equation of state
E-SCAPE European Scaled Pool Experiment
EPR   European Pressurized Reactor
ESFRI European Strategy Forum on Research Infrastructures
ESS   European spallation source
FAs   Fuel assemblies,
FP5   5th Framework Programme;
FP6   6th Framework Programme
GUI   Graphical user interface
Hades High-Activity Disposal Experimental Site
HEU   Highly Enriched Uranium
HLM   Heavy Liquid Metal
HLMC  Heavy Liquid Metal Coolant
HX    Heat exchanger
HXs   Heat exchangers
IAEA  International Atomic Energy Agency
IBA   Ion beam applications
LANL  Los Alamos National Lab
LBE   Lead-bismuth eutectic
LFR   Lead Fast Reactor
LHMA  Laboratory for High-and Medium-level Activity
LLFP  Long-Lived Fission Products
LLNL  Lawrence Livermore National Laboratory
<table>
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<tr>
<th>Acronym</th>
<th>Description</th>
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<tbody>
<tr>
<td>MAG</td>
<td>Minister Advisor Group</td>
</tr>
<tr>
<td>MAAs</td>
<td>Minor Actinides</td>
</tr>
<tr>
<td>MOX</td>
<td>Mixed oxide fuel</td>
</tr>
<tr>
<td>MYRRHA</td>
<td>Multi-purpose hybrid research reactor for high-tech applications</td>
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<tr>
<td>MTRs</td>
<td>Material Testing Reactors</td>
</tr>
<tr>
<td>NITA</td>
<td>Non-iterative time advancement</td>
</tr>
<tr>
<td>NRG</td>
<td>Nuclear research group</td>
</tr>
<tr>
<td>PET</td>
<td>Positron emission tomography</td>
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<tr>
<td>PSB</td>
<td>Proton synchrotron booster</td>
</tr>
<tr>
<td>PWR</td>
<td>Pressurized water reactor</td>
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<tr>
<td>R&amp;D</td>
<td>Research and Development</td>
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<tr>
<td>SCK•CEN</td>
<td>Studiecentrum voor kernenergie - centre d'étude de l'énergie nucléaire</td>
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- $\Phi$ : Flux
- $\Delta$ : Variation
- $\beta$ : Expansion coefficient
- $\rho_0$ : Reference density
- $C_R$ : Hydraulic resistance coefficient
- $b$ : Bouyancy
- $k$ : Pressure loss coefficient;
- $n$ : Neutron;
- $c_p$ : Specific heat capacity
- $p$ : Proton
- $eff$ : Effective;
- $f$ : Friction;
- $D$ : Diameter [m];
- $D_{hyd}$ : Hydraulic diameter
- $L$ : Length [m];
- $Ac-s$ : Cross flow area;
- $R$ : Proportionally constant
- $T$ : Temperature [K] o [°C];
- $p$ : Pressure [bar];
- $Nu$ : Nusselt number [-];
- $Pe$ : Nusselt number [-];
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>Ri</td>
<td>Richardson number [-]</td>
</tr>
<tr>
<td>Re</td>
<td>Reynolds number [-]</td>
</tr>
<tr>
<td>Fr</td>
<td>Froude number [-]</td>
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<tr>
<td>Eu</td>
<td>Euler number [-]</td>
</tr>
<tr>
<td>H.S</td>
<td>Heat source number [-]</td>
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Chapter 1. Introduction

In recent years, the energy problem is perceived as a priority for sustainable development of industrial society. The development and economic growth pose the problem of a continual increase in energy demand in the face of increasingly limited availability of fossil fuels.

Without adequate energy supplies humanity cannot effectively counteract the disastrous effects of climate change or social-economic disparities between industrialized and developing countries. To cope with these scenarios as effectively and timely as possible, it becomes essential to diversify energy sources and the development of new technologies which can improve the conversion efficiency with consequent benefits in terms of lower consumption of raw materials and environmental impact of energy systems.

A fundamental role in this direction can be assumed by nuclear energy which currently represents the only truly alternative and complementary energy source to fossil fuels, as it can produce when and where necessary, in sufficient quantity the energy required to sustain development, and its supply can be enough to provide adequate time for the possible development of other technologies.

About a third of the electricity is produced by plants in Europe that use nuclear fuel [1]. Despite the abundant availability of this resource and, consequently, the security of supply, it is always relevant to the debate on its use, particularly for the questions posed by disposal of radioactive waste and safety of operation, especially after the recent Japanese events. The future of nuclear energy and the consensus of public opinion will depend strongly on the level of safety reached by these facilities and the ability to find solutions best suited to safely manage the problem of radioactive waste. In relation to these aspects, the need is felt to define new strategies for sustainable development in the nuclear field, improving the technology of reactors and by starting research programs to make more socially acceptable the problem of highly radioactive waste. The interest in sustainability, understood as resource preservation, environmental protection and ability to ensure the ability of future generations to meet their own needs, is the main goal of fourth generation reactors [2].

One of crucial point in discussion is, as mentioned, the management of long-lived radioactive waste, with particular interest in the possible treatment techniques of the fuel before it is finally disposed of in storage. Special attention is paid to the transmutation of fission products and transuranic long-lived radioactive nuclides, with reference to the transmutation of these isotopes into others characterized by shorter lives. The systems currently under study for the transmutation of long-lived radioactive isotopes such as Pu and minor actinides (Am, Np), are
mainly of three types: light-water thermal reactors, fast neutron spectrum reactors, operating in critical conditions, and multiplying subcritical reactor systems driven by an accelerator (ADS) [3]. The latter type has more than the critical systems in terms of safety in the transmutation process, as introducing into a critical system minor actinides decreases the fraction of delayed neutrons resulting in problems of management and control of the system [4]. For these reasons, the subcritical reactor types have received interest in Europe; in fact, since 1998, different Countries, as France, Italy and Spain, realized the potential that could have an ADS for nuclear waste treatment and, for these reasons, established the Working Group (MAG Minister Advisor Group) with the aim of establishing a European platform for research and development of ADS. With the meeting of the MAG March 1, 1999, it was recognized as a European objective the construction of an Experimental Accelerator Driven System (X-ADS). Within the 5th Framework Programme, FP5 (1998-2002), the European Union defined priorities for Europe-wide research and development [5], and we have moved to the detailed project of X-ADS in order to assess the technical and economic feasibility. In the 6th Framework Programme (2002-2006) [6] and 7th (2006-2010) the focus was then directed towards research and development of techniques for more efficient burning of the fission products, with the ultimate aim of achieving a sufficiently safe, economic and with a production of wastes much lower compared to previous generations (XT-ADS) [4]. Moves in this direction were made in the different European research projects; one of the most interesting projects is MYRRHA, presently in an advanced stage of development at the Belgian research center “SCK·CEN”. The MYRRHA project contribution to a possible implementation of the ADS concept consists essentially in the feasibility study and analysis of safety in the use of the Pb-Bi eutectic alloy as a coolant in the reactors and ADS spallation target for the proton beam, as well as for the study of transmutation of minor actinides and long-lived fission products [7]. Since liquid metal is used as a core coolant and spallation target material, knowledge of the thermal-hydraulics phenomena in the reactor pool and of the thermal-hydraulic behavior of the liquid metal is essential for the design of ADS [8]. In this work, an overview of the main thermal-hydraulic issues occurring with LBE in a liquid metal pool reactor is presented. Model experiments are necessary for this understanding of physics, for validating numerical tools and to develop the measurement techniques.

In the frame of the THINS Project [9], the E-SCAPE (European Scaled Pool Experiment) facility under design at SCK·CEN is a thermal-hydraulic scaled model of the MYRRHA reactor and will address key phenomena relevant for liquid metal cooled reactors.
In this thesis, the design of the experimental facility was supported, studying a scaling methodology and carrying out a thermo-hydraulic analysis using the RELAP 5 system code. The design of a test facility cannot completely satisfy all the ideal scaling requirements. Thus, scaling distortions are generally inevitable. Distortions are encountered for two major reasons:

- difficulty to match the whole set of scaling criteria;
- lack of understanding of the local phenomenon itself.

In this case, the qualification of system thermal-hydraulic codes against scaling remains an open question [8].

The main goal of this thesis work, conducted in cooperation with SCK·CEN staff, is to give a significant contribution to the system code qualification and to the final development of the MYRRHA reactor, at the time considered the leading European project for the passage that the ADS will have from a test phase to a development on an industrial scale.
Chapter 2. MYRRHA – a Multi-purpose Hybrid Research Reactor for High-tech Applications

2.1 Belgian nuclear research centre (SCK•CEN)

The Belgian nuclear research centre or studiecentrum voor kernenergie - centre d’études de l’énergie nucléaire (SCK•CEN) was founded in 1952 in order “to carry out all research regarding applications of nuclear power and to promote and encourage such research by any means possible” [7]. The institute has played a pioneering role in the nuclear sector ever since.

One of the SCK•CEN core competences is the conception, design and realization of large nuclear research facilities such as the BR1, BR2, BR3 & VENUS reactors, the LHMA (Laboratory for High-and Medium-level Activity), hot cells, or the Hades (High-Activity Disposal Experimental Site) underground research laboratory for waste disposal [10]. SCK•CEN has then operated these facilities successfully thanks to the high degree of qualification and competence of its personnel and by inserting these facilities in European and international research networks, contributing hence to the development of crucial aspects of nuclear energy at an international level.

Some of the most relevant SCK•CEN institute milestones, concerning the three nuclear installations in SCK•CEN domain, are reported:

- On May 11, 1956 the Belgian Reactor 1 (BR1) became critical for the first time and as such it is the oldest research reactor in Belgium. It is a graphite-moderated, air-cooled reactor with a maximum thermal power of 4 MW and is fueled by natural uranium. The reactor is still used for research and for educational purposes as well [7].
- On July 6, 1961 the Belgian Reactor 2 (BR2) was started up for the first time. It is a high-flux research reactor with a beryllium matrix, and fueled by highly enriched uranium. Up to this day, having been refurbished twice, it is still one of the highest performance research reactors in Western Europe. It is also used for the production of medical isotopes and the irradiation of silicon to obtain n-doped silicon by transmutation [11].
- On August 19, 1962 the Belgian Reactor 3 (BR3) was put into operation for the first time and was the first PWR (Pressurized Water Reactor) in Western Europe. BR3 was a demonstration unit of an industrial power station and served as a test reactor for
prototype nuclear fuels. Within the framework of the European five-years program for research and technological development for the decommissioning of nuclear installations, BR3 was chosen, next to three other European installations, as a pilot project for the demonstration of the decommissioning of PWR plants. These projects aim to develop the necessary scientific and technical knowledge for decommissioning plants in real conditions. According to plan, the plant will be completely decommissioned by the end of 2011 [7].

At present, the SCK•CEN is again working at the frontier of nuclear knowledge with the MYRRHA project. MYRRHA would be a 50-120 MW\textsubscript{th}, sub-critical accelerator driven system (ADS). The main purposes of the MYRRHA project can be summarized as follows:

- research on transmutation of Minor Actinides by proving the ADS technology;
- research on materials and nuclear fuel;
- production of medical isotopes and semiconductors doping;
- demonstrative facility for the Lead Fast Reactor (LFR) technology.

On March 4, 2010 the MYRRHA project received the "go ahead" by the Belgian government. The same day, it was announced that MYRRHA would be included in the 2010 priority list of the subgroup Energy of the European Strategy For Research Infrastructures (ESFRI) [10].

Figure 1. View in order of the BR1, the BR2, the BR3 and a MYRRHA model.

2.2 The role of MYRRHA within present-day needs of nuclear community

The need for a safe and economic operation of the European nuclear park for preserving the natural resources, demands the start-up of one or more new MTRs (Material Testing Reactors) in a very near future. On the other hand, the closure of the fuel cycle as well as the
development of future energy systems clearly requires the use of fast spectrum irradiation facilities. The latter are also very scarce in Europe and with very short perspectives (PHENIX and BOR-60 (Ru)) [10].

The MYRRHA project started at SCK•CEN in this context: the concept of a new irradiation and testing facility to replace BR2. MYRRHA, being a fast neutron spectrum facility, would be also a natural complementary project to the thermal spectrum MTR Jules Horowitz (RJH project) facility in France. That would give in Europe a full research capacity in term of nuclear R&D (Research and Development) and reliability in radioisotopes production for medical applications where networking of irradiation facilities is a must [10].

The MYRRHA project started in 1998 as an upgrade of ADONIS concept [10], which has been studied from 1995 up to 1997 by SCK•CEN in collaboration with Ion Beam Application (IBA). ADONIS was a small irradiation facility, based on the ADS concept having the dedicated objective to produce radioisotopes for medical purposes and, in particular, $^{99}\text{Mo}$, based on HEU (Highly Enriched Uranium) fissile targets. Besides the medical purposes of ADONIS, MYRRHA is designed as a multi-purpose facility [10] in order to support research programs on fission and fusion reactor structural materials and nuclear fuels for ADS, but also for critical reactors with higher burn up limits or for next generation reactors.

To put all features and possible applications of MYRRHA into context, a brief overview of present-day issues and needs of the nuclear community is here presented.

- **Nuclear energy**

The public acceptance of nuclear energy is largely influenced by the problem of the radioactive waste and safety, especially after the recent Fukushima events. There are several pathways for waste solution currently envisaged. One solution is the immediate geological storage without reprocessing. Another possible solution consists in the reprocessing, where the fissile material is recovered for being re-inserted in the fuel cycle, and the amount of nuclear waste that has to be stored is reduced considerably. However, geological storage would still be needed. It is important to consider how one of the most sensible issues for the public remains, however, the extensive amount of time required to reach an acceptable level of radiotoxicity for the waste [11].

A third option consists in the partitioning and transmutation of the nuclear waste by irradiation of the wastes with an intense neutron flux in order to transmute Long-Lived
Fission Products and Minor Actinides to isotopes that decay to a stable form within a time scale reduced by several orders of magnitude. Constraints on geological storage could thus be reduced substantially.

One of the MYRRHA goals is just to conduct further research on transmutation of nuclear wastes. MYRRHA as a multipurpose irradiation facility will also provide material studies for the current park of Gen. II nuclear power plants, for the Gen. III plants, such as the AP 1000 and the EPR currently under construction from France to China, and for the next generation, Gen. IV, currently under study and design.

- **Nuclear medicine**

Radio-isotopes are widely used in medicine for diagnostic purposes. One of the most popular examples is Tc-99m used in positron emission tomography (PET) [11].

The Tc-99m is built into a molecule which is injected into a patient's blood stream. During its stay in the body, the Tc-99m isotope decays (half life about 6h) and two gamma rays are emitted back-to-back. By using a gamma ray detector, one can visualize the entire metabolism of the molecule the Tc-99m was embedded in. By selecting molecules with a high uptake by a specific tumor, one can visualize them. Figure 2 shows a perfusion scan of a patients lungs. The Tc-99m is in this case built into a macro-aggregated albumin and the blood flowing in the lungs is visualized. One can clearly see the left lung having a deficient blood flow.

Tc-99m is a decay product of Mo-99 (half life 66 h) which is a fission product. Currently there are only 5 research reactors in the world, producing 95% of the world supply of Tc-99m. The BR-2 reactor at the SCK•CEN is responsible for 15% of the production. The other reactors are the HFR in the Netherlands, OSIRIS in France, NRU in Canada and SAFARI in South Africa [11].

![Figure 2. Tc-99m perfusion scan.](image)

A series of events has recently shown how thin this production is stretched. In February 2010, the HFR went off-line for six months, while the NRU was off-line until April
2010 as well. In order to counter the threat of a global Tc-99m shortage, the capacity of the BR-2 was boosted in order to cover 40-65% of the weekly basic world need [11].

In time, MYRRHA would take over the production of medical radio-isotopes after the shut-down of the BR-2 and continue to maintain Belgium's world leader position in this field.

- **Semiconductor doping**

Experimental rigs in the MYRRHA core will also be used to perform neutron transmutation doping of silicon. This technique allows for the precise creation of uniform n-type semiconductor through nuclear reactions in a pure silicon crystal under neutron irradiation:

\[
^{30}\text{Si}(n,\gamma)^{31}\text{Si} \rightarrow ^{31}\text{P}
\]  

(1)

Subsequent radiation damage of the lattice structure can easily be removed by thermal annealing [12].

- **Development of Fast Spectrum Reactors and ADS**

Present-day nuclear reactors are all critical reactors, with a large majority of thermal spectrum reactors. They have an effective neutron multiplication factor of 1, i.e. one neutron induces the fission of a $^{235}\text{U}$ or $^{239}\text{Pu}$ atom and from the neutrons emitted in that fission process, only one will lead to a new fission reaction. The other neutrons are lost by absorption in coolant atoms, structure materials, scattering out of the core, etc..

On the other hand, the fuel exploitation of such kind of reactors is about 1%. A fast spectrum reactor is characterized by a different neutron spectrum and by a neutron flux about 100 times higher than a thermal spectrum reactor: it could lead to a better fuel exploitation (theoretically until 70%) and to an increase of the reactor burn-up.

A fast spectrum reactor is also necessary to burn all MAs (minor actinides) which should not be fissionable with a thermal spectrum [13].

The MYRRHA reactor will be the first working full-scale Accelerator Driven System (ADS) in the world. Although the concept of ADS is relatively old (it was first mentioned by Nobel prize laureate E.O. Lawrence in the 1950's) it is only recently that technological advances in particle accelerators are sufficient to enable its realization [10].
MYRRHA/XT-ADS design has been conceived with a sub-critical core, with an effective neutron multiplication factor of about 0.95 [10]. This means that the core cannot sustain the fission chain reactions on its own. An external neutron source is required to keep the reactor going. Sub-criticality is also necessary because efficient transmutation requires fast neutron spectra, but fast reactors with an excessive load of Major Actinides could present some issues from the point of view of control: the delayed neutron fraction is low, which makes them difficult to control in critical mode. Because of sub-criticality, more actinides can be transmuted in an ADS without decreasing the controllability. Thus, this type of reactors is inherently safe: switching off the proton beam is enough to fade-out the fission reaction [14].

The first United States project (Accelerator Transmutation of Waste) dates back to 1996, and it was based on thermal neutrons utilization for transmutation [5]. In Europe, the research activities started in 1993, when a scientists group driven by Carlo Rubbia in the CERN research centre of Geneva presented a base concept of a sub-critical system, able to product energy with a very low level of Minor Actinides and Long Lived Fission Products production, based on a U-Th cycle coupled with an highly energy protons accelerator.

The ADS is a reactor type which uses an high energy proton accelerator. The accelerator absorbs about 10-20 MW of power and accelerates the protons until reaching energies of about GeV, with proton beam intensity of about 10 mA [13]. The high-energy protons will undergo mainly (p,xn) reactions upon impact with metal atoms generating a reaction called spallation reaction that “crushes” the target nucleus in lighter nuclei and generates the emission of about 30 neutrons per each nucleus scattered.

The spallation flux intensity reaches values of $10^{14} - 10^{15}$ n/(cm$^2$·s). These neutrons generated by the reaction will then be used for fission reactions in the fuel blanket surrounding the spallation target. The spallation neutrons energy spectrum is dominated by ‘evaporation neutrons’ (about 90%), which have a medium energy of the order of a few MeV and are emitted by nuclei disexcitation after the reaction. The spallation target is surrounded by FAs (fuel assemblies), which constitute the subcritical core ($k_{eff} = 0.95$). The FAs located closest to the spallation target could be partially loaded with special fuel containing a relatively high amount of MAs in order to allow the transmutation in the region where the neutron flux is higher. The cooling is normally done by liquid metals for sub-critical fast reactors.

The short life fission products are directly sent to storage sites; a lot of research activities are at the moment focused on increasing the minor actinides and long life fission
products separation efficiencies. The transmutation efficiencies is also strongly influenced by neutron economy and by energy spectrum of neutrons present in the reactor.

A fast spectrum reactor is necessary to burn all MAs which are not possible to burn with a thermal spectrum [3]. The MYRRHA main target is to bring a major contribution in order to prove the ADS concept at a reasonable power level and, on the other hand, the technological feasibility of transmutation of MA and Long-Lived Fission Products (LLFP), present in LWR spent fuel and generated from reprocessing activities [14].

2.3 Lead-bismuth technology

2.3.1 Development

The utilization of Heavy Liquid Metal (HLM), as cooling medium and as neutron spallation target has been considered advantageous in the field of Accelerator Driven System (ADS) and in other research or industrial topics as for instance the energy production with advanced nuclear systems, the hydrogen production with nuclear power plants, and the development of spallation neutron sources for medical applications and materials investigation like largely shown above.

It is therefore evident that the rising attention of HLM in these activities, needs a scientific and technological support to thoroughly characterize the HLM applications. According to this scope, important experimental international projects involving HLM studies are carried out. We mention here some of the most important:

- **FAst Spectrum Transmutation Experimental Facility (FASTEF).**

In the frame of the project CDT of the 7th European Community Framework Program, 20 European Organizations (coordinated by SCK•CEN) have the strategic objective to further develop the design of a FAst Spectrum Transmutation Experimental Facility (FASTEF) able to demonstrate efficient transmutation and associated technology through a system working in subcritical and/or critical mode (Pilot Plant for both ADS and LFR).

The research activities carried out within the MYRRHA-FASTEF project are focused mainly on compatibility of materials with the LBE and thermal-hydraulic issues connected. A conceptual design of a scaled down facility with respect to the reference plant is the European Scaled Pool Experiment (E-SCAPE) under design at SCK-CEN research centre [9, 14, 30].
• **Lead-cooled European Advanced DEMonstration Reactor (LEADER) project.**

In the 7th European Community Framework Program, the LEADER project started on April 2010. The objectives are the analysis of the hard points of the ELSY design in order to identify possible improvements with the goal to reach a feasible and improved LFR configuration, and the definition of a new “frozen” LFR configuration to be used as a reference plant. A scaled down facility respect to the reference plant is the ALFRED demonstrator, with a size of the order of 120 MWe (to be representative with limited cost) [15].

• **Impianto Sperimentale Sottocritico, low-power nuclear facility (Legnaro, Italy)**

It includes experimental facilities/loops for materials development, components design, systems development and codes qualification/validation [15].

The European THINS Project will establish a new common platform of research results and research infrastructure. The project will achieve optimum usage of available European resources in experimental facilities, numerical tools and expertise. The main outcomes of the project will be a synergized infrastructure for thermal-hydraulic research of innovative nuclear systems in Europe [16].

### 2.3.2 Thermal hydraulics

The advantages of lead-bismuth and lead reactor cooling are high boiling temperatures and the relative inertness to water as compared with sodium. The melting and boiling points of sodium are 98 and 830 °C, respectively. For lead-bismuth eutectic, this values are 129 and 1670 °C, respectively, while for pure lead 327 and 1740 °C at atmospheric pressure [17, 18].

The boiling points are well above cladding failure temperatures. The specific heat per unit volume of lead-bismuth and lead are similar to that of sodium but the conductivities are lower about a factor four [18]. The use of LBE (Lead-bismuth eutectic) as target material gives also some advantages if the core coolant is LBE as well. It is due to the better temperatures compatibility occurring to core and target regions and also to a better chemistry compatibility between primary coolant and target coolant if for some reason they are brought into contact.

HLMC (Heavy Liquid Metal Coolant) are low Prandtl number fluids with markedly different thermal hydraulics and heat transfer characteristics from those of water. At present,
correlations obtained from liquid sodium metals R&D are applied without substantial verification and modification to HLMC. In cases of calculating natural circulation in simple piping systems and heat transfer in heat exchangers such use appears to be warranted, with possibly a 10-20% reduction in Nusselt number correlation obtained from sodium experiment. A recent study showing an increase in thermal impedance due to oxygen in LBE suggests that non-wetting may contribute 20% or more reduction in heat transfer [17].

Two IAEA reports contain a very comprehensive compilation of Lead Bismuth thermal hydraulic properties and recent studies in this field [18-19]. However, the available data are not yet sufficient for complete validation of CFD (Computational Fluid Dynamics) codes and for development of reliable and realistic physical models [17].

Many lead-alloy cooled nuclear systems incorporate significant natural convection potential for main operation or enhanced passive safety, and the core designs are usually based on open-lattice configuration. 3-D natural convection distribution in pool-type systems, its stability and transients, and main circulation and flow enhancement mechanisms (mechanical and electromagnetic pumping, gas and steam lift) need to be carefully investigated, developed and tested.

2.3.3 LBE technology

The LBE technology ensuring measurement and maintenance of the LBE quality is required during its long time operation both under normal conditions of leak-proof circuit and in case of partial loss of integrity of the circuit during repair and reactor refueling. Functioning of these systems and devices are necessary for eliminating structural material corrosion and circuit slagging by the lead oxides [20].

The most common cause for corrosion damage, which is the most dangerous for structural materials, is local corrosion of materials appearing as the separate corrosion-erosion centers (“pitting”). Local corrosion damages of structural elements may appear at temperatures over 550 °C after holding for some hundred hours under the following conditions: unbalance of alloying elements and impurities in steel, poor quality of metal, absence of coolant quality control and non optimal coolant flow regimes. The typical corrosion rate in such cases is estimated as 2.55 mm/year [20].

The principal solutions ensuring high corrosion resistance of structural materials in heavy liquid metal coolant were found using oxygen dissolved in the coolant [17]. It has been shown as a result of long term studies that this corrosion resistance essentially depends on the
concentration and dissolved oxygen. Upon reaching certain level of concentration of dissolved oxygen, corrosion processes are stopped due to protective oxide film formed on the steel surface. At high temperatures the presence of silicon in steel as additional alloying element is a very important condition of corrosion inhibition. The silicon content in steels is varied within 1-3.5% range depending on steel type [17].

Oxide films formed on the steel surface prevent it from interaction with liquid lead. Since breakdown of film is possible during operation, precaution must be taken for resuming and maintaining their thickness and density. Thus, steel corrosion in molten lead can be significantly slowed down by the oxide film formed on the steel surface.

Maintaining such oxygen content in the coolant would provide stability of oxide film \((Fe_2O_4)\) on the steel surfaces, but would also preclude lead oxide (PbO) generation in the coolant, that could result in the circuit slagging. There are some ranges of content of oxygen dissolved in lead in meeting these two conditions, for instance \((5 \cdot 10^{-6} - 10^{-3}\text{ wt\%})\) range [17]. Oxygen content in lead can be controlled either injecting gaseous oxygen or by dissolving solid PbO. The general conclusion of several experimentations and tests may be drawn as the following [17]:

- For unprotected steels (no coatings), the necessity and efficacy of oxygen control are validated in all tests, i.e. in-situ growth of surface oxide layers on steels in LBE with sufficient concentration of oxygen significantly reduces corrosion.

- In static tests within the oxygen control band, most martensitic and austenitic steels form oxides that are protective under ~550 °C, especially for oxygen concentrations above \(10^{-6}\text{ wt}\%\).

- In dynamic tests, most of which are in LBE and the oxygen concentrations are in \(10^{-6} - 10^{-5}\) wt\% range, the austenitic and martensitic steels formed protective oxides.

- Between 550 and 600 °C, the formation and protectiveness of oxides on martensitic steels are uncertain for durations up to a few hundred hours, but usually fail after that. For austenitic steels, the oxides are thin and not completely protective at ~550 °C.

It appears evident that the oxygen control is a critical issue for development of HLMC technology. The ability to effectively measure and adjust the oxygen activity in Pb/LBE is
critical for the HLMC technology [21]. Achieving this control is more challenging than control of oxides in sodium cooled systems, but has proven readily achievable in many test facilities around the world via different approaches. With special steels and/or coatings, it is hoped that the range of control can be relaxed.

Practical implementation varies from lab to lab, each with its own advantages and disadvantages. However, if reliable monitoring is available, the long time constants in the processes affecting corrosion (days) and slagging (hours) should give operators sufficient time for control and adjustment. The main oxygen control devices are mentioned here [22]:

- A gas phase oxygen control

In equilibrium, the partial pressure of oxygen in the cover gas space above LBE should be equal to that in LBE, when the concentration in LBE is below saturation. There are two methods that can be implemented in practical systems to control the oxygen activity in LBE/Pb.

1. Direct injection of oxygen and hydrogen gases

With large volumes in many LBE systems, including test loops, it is expedient to inject oxygen and hydrogen directly when depletion or excess of oxygen is detected by the oxygen sensors. These gases are typically mixed with a cover gas (inert, helium or argon) to regulate the pressure. Excess oxygen reacts with hydrogen to form steam and exits through the exhaust gas line. This is the method implemented in the DELTA Loop at LANL.

The advantages of this method include: the setup and procedures are simple, the input and output are in gas forms with easy addition of gas supplies and no solid residues, and it is applicable either as injection into LBE through a bypass loop, or over a flowing LBE pool where the mass exchange rates are favorable. The hydrogen injection line can be used in high flow capacity to clean up oxides and restore the thermal hydraulic performance of the LBE systems after prolonged operations.

The disadvantages include the need for careful design and control of the gas line pressures and seals, the low mass exchange rate between liquid and gas (leading to long time constants and difficulty in tight control), and the excess of oxygen at the inlet (if directly injecting into LBE) during addition of oxygen that leads to plugging, or slag formation which can be transferred and settled in various areas and is difficult to clean up.

- Injection of hydrogen and steam gas mixtures

The low concentration of oxygen needed for the control technique makes it nearly impossible to supply oxygen at the right level directly. However, such low level of oxygen can be
achieved in certain reaction systems, e.g. hydrogen and water, or CO and CO\textsubscript{2} mixtures. For versatility and safety reasons, hydrogen and water system is used for practical applications. In practical implementation of this method, a hydrogen and cover gas (He or Ar) mixture passes through a temperature-controlled water bath to pick up water vapor at the desired levels. The resultant hydrogen/steam mixture can either go directly into LBE system to complete the reaction there, or go through a high-temperature reaction chamber to reach thermodynamic equilibrium (hence the desired oxygen level) beforehand. The advantages and disadvantages of this method are similar to that of the direct gas injection method, with the additional advantage of having the correct level of oxygen delivered to LBE, and that the gas system can double as a calibration system. The additional disadvantage is that the implementation is slightly more complex, and since there is equilibrium control, large and rapid adjustment (e.g. at start-up, or after abnormal events) is difficult. So it may need to be supplemented with direct injections.

These methods are becoming increasingly popular in newer experimental and test facilities, with good implementations in FZK, CEA, Tokyo Tech, among others. For both gas phase control methods, it is important to have some free surface with robust flow or agitation for efficient mass transfer between the gas and liquid metal, and rapid response for easy control.

- **Solid phase oxygen control**

The Russian experience suggests that to avoid excess slagging in the process of using oxygen to passivate the structural material surfaces, a solid mass exchange device should be used. The mass-exchanger consists of a porous canister filled with mechanically stable heat-resistant spheres of lead and bismuth oxides. The mass-exchanger is installed in an LBE flow bypass in the coldest part of an LBE system. By controlling the coolant flow rate (adjusting bypass valve opening), temperature and bypass opening duration, the oxygen concentration in LBE can be controlled. Since there is no solid oxide being discharged into the LBE systems, excess slagging is avoided [23].

The advantages of this method are the efficiency of mass exchange between solid and liquid phases, and the avoidance of slagging, which leads to cleaner operation over longer periods of time and less degradation of the thermal hydraulic performance. This may be considered a special “cold-trap”.

The disadvantages include having to insert a fixed reserve of solid oxides into the LBE systems that, if depleted, is harder to replenish. In any case, a hydrogen injection line may still be necessary to reduce excess oxides and restore the systems.
However, it may be possible that by adding some hydrides into another bypass to form another mass-exchanger, a hydrogen gas line will not be necessary.

In LBE systems with relatively short operating lifetimes, e.g. spallation targets in which radiation damage is the limiting factor, using solid phase mass-exchanger to control oxygen has the advantage of eliminating some gas lines and exchanges, enhancing containment and safety.

The R&D needs for solid phase oxygen method is more extensive. The forms of lead and bismuth oxides, the configuration of the mass-exchanger, and the exchange rate and dependence on flow and temperature will need to be selected and studied. The potential improvement in performance and safety, however, makes its development imperative.

Polonium produced in irradiated bismuth had been considered a major problem in LBE coolant applications. Po-210 is a strong alpha emitter hence a serious safety concern in LBE-cooled nuclear systems. There have been documented experiences and health effects in handling polonium in former Soviet Union's nuclear submarine program [21], and US nuclear program [22]. The main conclusions are summarized below.

Polonium is known to exist both in the elemental form and in chemical compounds such as polonium oxide, intermetallic compounds, polonium hydride, etc.. In solutions the Po volatility is usually substantially lower than the vapor pressure of elemental Po at the same concentration, suggesting it mainly exists in less volatile compounds. For instance, studies indicate that Po exist mostly in the form of lead polonide in LBE and Pb-17Li, reducing Po evaporation rate by 2–4 orders of magnitude. Inert gas can further reduce the sublimation rate of Po by as much as $10^6$ times less than that of metallic Po in vacuum. However, Po can also form unstable gaseous compound, namely polonium hydride, with the moisture in air. This will increase the release of PoH$_2$ from solidified LBE in moist air [22].

During normal operations with closed systems, polonium poses no safety hazard based on the experience and analysis of nominal leakage. During opening of primary coolant systems for maintenance, repair and refueling, or accidents of coolant leaks and spills, the danger level is substantially elevated. Specifically, primary coolant system breach will contaminate secondary coolant with polonium, and release polonium in aerosol form into air in compartments and onto surfaces. Unloading core will also lead to elevated polonium levels in the reactor compartment. In addition to the need to establish access controls and personnel monitoring, Russian experience shows the efficacy of using polymer films to fix and remove released polonium that can be used with other containment and removal methods. MEGAPIE
project decided to adopt double containment and early leak detection to mitigate potential hazards. A study of a large cohort of nuclear workers in US Mound Facility (4402 men during 1944–1972) showed no elevated mortality, or significant dose–response trends [24].

In Russia’s SVBR-75/100 program, it is estimated that the Po-210 release from LBE will not exceed Russia's personnel permissible values with a nominal 0.5%/day cover gas leakage. It is thus reasonable to conclude that engineering and administrative controls can effectively mitigate polonium hazards [22].

2.4 Thermal-hydraulics characteristics of liquid metal pool reactors

In this section, an overview of the main thermal-hydraulic phenomena occurring in a liquid metal pool-type reactor are summarized [25]. An overview of these topics is necessary to support the reactor design and safety studies.

- **Subassembly thermal hydraulics**

Thermo-hydraulic performance prediction for the core subassemblies begins with calculation of the subassembly flow rate necessary for determining the limiting design parameter (e.g. lifetime or maximum local cladding temperature) in the limiting subassembly of each flow orificing zone. Total calculated subassembly flow rate and design data on core neutronics are input to sub-channel analysis codes that predict coolant flow and temperature distribution in the sub-channels of the core subassemblies.

Most fast reactor subassembly concepts consist in a pin bundle with helical wire spacers [25]. Instead of helical wires in some cases grids can be used. The pin bundle is surrounded by a hexagonal can and the flow is distributed into the fuel assembly by holes connected to the cold plenum at the bottom.

The main parameters to be evaluated are the total subassembly pressure drop, the peak factors, the maximum coolant temperature, the clad temperature distribution and especially the clad maximum temperature, the maximum fuel temperature, the hexagonal can temperature for thermo-mechanical analysis. These parameters are important for steady-state conditions and for transient situations as well [25].
Determination of the maximum clad temperature requires an accurate knowledge of the global and local thermal hydraulics in the pin bundle. Maximum permissible coolant velocity, flow rate, and the clad depth are also important parameters to take into account. Another important feature is the flow distribution and the mixing induced by the wire-wrapped spacer which imposes local thermal hydraulic coupling between the sub-channels. It is necessary to take into account three kinds of sub-channels which have different section areas: inner, edge and corner channels. Since the inner channels show the lowest hydraulic diameter (\(\rightarrow\) highest pressure drop), the mass flow rate through these channels will be reduced. The other parameters which influence the clad temperature are the axial, radial and local power distribution in the core and in the pin bundle [26].

- **Core thermal hydraulics**

The global core thermal hydraulic behavior must be studied for design and safety purposes [25]. Inter-channel heat and mass transfer is a very important factor in generation of temperature and flow behavior. One of the main targets is to obtain, especially at nominal power, a uniform fuel core outlet temperature although the radial profile of neutron flux is largely non-uniform [26].

The temperature on the hexagonal tubes also needs to be studied for a thermo-mechanical analysis. This information requires a good evaluation of the inter-wrapper flow behavior and the temperature field in this region [26]. The inter-wrapper zone may be fed by a by-pass flow adjusted at the bottom of the subassemblies. Another important phenomenon to be taken into account is thermal stratification which should occur (especially in natural circulation flow) and induce thermo-mechanical stresses on the hexagonal tubes due to the axial temperature gradient.

- **Upper Plenum thermal hydraulics**

Many thermo-hydraulic challenges are concentrated in this region [25]. At the core outlet level, an important safety requirement is the validation of the core outlet temperature measurements. It is quite important to be sure that the thermocouples measure the effective core outlet temperature for all operating conditions. The measured temperature is sensitive to local characteristics of the core upper region [25].
The radial temperature differences on the upper core support plate, at the core outlet level, is an important feature to be evaluated: if temperature fluctuations of high amplitude (several tenths of Celsius) can be produced by the mixing process, with a large range of frequency, consequently thermal fatigue may occur and induce thermal striping at long term on the concerned structures such as subassembly heads, thermocouples, supporting grid, etc. [27]

Another important issue is represented by the free surface oscillations which should occur as a consequence of the hydraulic behavior in the upper plenum. Reducing the vessel dimension for a given power and a total flow rate, for example, makes the free surface oscillations more significant [25]. The thermal gradient oscillations at the free surface need to be reduce to avoid the risk of thermal fatigue.

Another phenomenon that is important to avoid is thermal stratification. It occurs for example if a low flow or no re-circulating flow area is arranged (depending on the design features) or during a cold shock at the core outlet (scram for example) in which it can appear at a level located at the top of the heat exchanger window or on the internal structures. Thermal stratification can produce thermal stresses, which must be evaluated on the basis of the temperature gradient evolution [25]. Therefore, if a thermal stratification is present, the thermal interface may be unstable and thermal fatigue can be induced on the neighboring structures [27]. During some transient situations with the decrease of mass flow rate and temperature evolution at the core outlet, buoyancy forces may modify the flow pattern in the in the whole upper plenum [25].

- **Lower plenum thermal hydraulics**

Several important concerns must be analyzed in this respect. The lower plenum is including a low pressure region upstream to the pumps and a high pressure region downstream to the pumps [25]. The pump jet in the lower plenum may induce a significantly lower pressure on the diaphragm plate region which produces high stresses especially if also the temperature difference between cold plenum and hot plenum has to be absorbed by the plate. For this reason, for example, in MYRRHA reactor diaphragm two plates are foreseen [14].

An important case to consider is the non-symmetrical situation when one secondary circuit is not operating. The transient regimes can induce large changes in the lower plenum thermal hydraulic behavior. Buoyancy forces can play a significant role in many transient situations,
especially when the primary mass flow rate is reduced and the heat exchanger outlet temperature is largely modified [25]. In the case of a hot shock (loss of secondary heat sink for example), the jet at the outlet of the heat exchanger is raising by buoyancy and cold liquid metal remains stratified in the bottom of the pool. The evolution of the velocity and temperature fields should be studied for representative hot and cold shocks.

- **Decay heat removal**

Decay heat removal is a major challenge for all types of nuclear reactors. For LBE cooled fast reactors, passive decay heat removal based on natural convection is possible. This is one of the important advantages of these reactors. For decay heat removal with primary pumps stopped, for example, a natural convection regime occurs in the primary circuit. The whole transient procedure including the initial incident and the scram must be analyzed as it can induce, as previously mentioned, an initial thermal stratification in the upper plenum which will influence the later interaction between the immersed coolers and the hot pool [25]. The behavior in natural convection is a key point to demonstrate the reliability of the systems in case of a station black out. The design and the position of the immersed coolers are important parameters to optimize the system efficiency.

The purpose of removing decay heat is to remove the heat which continues to be generated in the core following reactor trip. The main heating sources present are for instance the decay heat in the reactor core, decay heat in the interim fuel storage, decay heat in the LBE (Po$^{210}$), decay heat from spallation products in the target unit.

The function must ensure that the heat is removed from the core to the ultimate heat sink. This is achieved with the fulfilment of the following sub-functions [28, 29, 30]:

1. Maintenance of the coolant inventory
2. Coolant circulation;
3. Pressure control in the primary circuit;
4. Maintenance of a coolable geometry in the core;
5. Heat removal from the primary coolant;
Another option could be a primary reactor auxiliary cooling system (PRACS) [28] if it is located in a position which allows natural circulation through the core as in the upper part of the intermediate heat exchangers.

- **Gas entrainment**

Gas entrainment in the primary circuit of liquid metal cooled fast reactors may lead to safety problems in case of accumulation and transport of gas through the core. A positive reactivity insertion should occur if a large quantity of gas is crossing the core. The most important scenario of gas production in the primary MYRRHA loop occurs in case of primary heat exchanger tube rupture: in this case the gas which exits from the pipe is very close to the pumps region and furthermore, characterized by an high pressure value. The presence of gas bubbles in the primary circuit can induce pump cavitation and the perturbation of possibly ultrasonic measurements. Other potential sources of gas are free surfaces, overflows and nucleation but because of the high LBE density, gas entrainment does not seem to be an issue in lead bismuth cooled reactors.

### 2.5 MYRRHA Reactor

#### 2.5.1 Overall configuration

In Figure 3 is shown the MYRRHA reactor vessel. The MYRRHA reactor in its latest design evolution (MYRRHA/FASTEF) will be able to operate in subcritical conditions but also in critical conditions. This flexibility is aimed to develop a number of common design solutions that could allow the reactor to efficiently operate in any mode, according to the selected operating conditions [30].

SCK\*CEN opted for a pool type system, in which the components of the primary loop (pumps, heat exchangers, fuel handling tools, experimental rigs, etc.) are inserted from the top through the cover [10, 28]. The configuration selected is an hanging vessel with hemispherical bottom. The pool Vessel is located in a nitrogen-controlled containment environment [10].
One of the most critical elements of MYRRHA is the spallation target that constitutes the physical and functional interface between accelerator and core. The goal of the spallation target is to create a neutron source able to feed the subcritical reactor core. To deal with a $k_{\text{eff}}$ of 0.95 a flux of about $10^{17}$ neutrons/s is required. These neutrons are generated by the spallation reaction, which occurs when the proton beam, with a current density of about $140 \mu \text{A/cm}^2$, hits the LBE target.

Because of the desired high fast flux and the high power density, SCK•CEN opted for liquid metal as coolant [10, 28, 29]. Sodium has not been chosen due to the fire safety reasons and to its chemical incompatibility with the Pb-Bi that is already decided as first choice for the spallation target material. Pb-Bi has finally being chosen as primary coolant because of its low melting temperature (129 °C), allowing the primary systems to function at rather low temperature levels. The low working temperatures constitute an approach to limit the corrosion problems due to HLM (Heavy Liquid Metal). The spallation target is thermomechanically loaded as well as subject to damages due to the impact with high energy particles [30]. The spallation module project is based on the optimization between neutron
economy, materials properties (physics and chemicals), and thermo-hydraulics performances imposed by safety and reliability.

Furthermore, a windowless target design has been initially selected, because of the high beam load on a hypothetical window that should sustain a proton current density of more than 140 $\mu$A/cm$^2$. In the meantime, the energy of the accelerator has been increased from 350 MeV to 600 MeV and more space is available for the spallation target loading. It also leads to a reduced heat deposition and material damage (dpa) in the target window. Furthermore, the experience of the MEGAPIE window and the advances in the material research justify a reconsideration of a design with a window. Re-opening the target loopless window option would lead to a simplification of the design of MYRRHA, gaining flexibility for in pile sections (IPS) position and, at the same time, reducing the investment cost. A first study indicates that a loopless window design option is a valid, simple, robust and flexible alternative for the complex windowless spallation loop design as it exist today in MYRRHA [28]. Analyses of material characteristics of T91 show that the preferential working temperature for the spallation window is found in the range around 450-500 °C. This range is desirable from the point of view of irradiation hardening, and liquid metal embrittlement and still acceptable for corrosion and material strength [30].

The increase in proton energy from 350 to 600 MeV is also a key parameter to make the thermal design of the beam window possible. The thermal load of the window and the spallation zone is reduced by a factor of two and even more. The design of the beam shape and the velocity distribution at the entrance of the spallation region, together with the reduced heat deposition, permits to choose the window temperature corresponding to the preferential working temperature. The alternative reactor design with a spallation window considerably simplifies the mechanical construction, not only of the spallation target but also of the surrounding structures (core support plate, core barrel and plug, reactor cover). Furthermore, it facilitates the maintenance operations and it improves the target’s reliability. It offers possibilities to occupy the freed room for e.g. Si-doping or a Pb-Bi conditioning system. Finally, the other systems parts (like the building) will also take advantage of the less complex spallation target.

The fast spectrum core is submerged in a liquid metal pool (with also lead as cooling fluid) and consists of a lattice of 99 hexagonal channels of which 45 are located with fuel assemblies housing 30wt% Pu-enriched MOX fuel pins arranged in a triangular pitch of 8.55 mm. The fuel rods have an active length of 60 cm. The $(\text{U-Pu})O_2$ fuel pellets have a density
of $10.55 \, \text{g/cm}^3$ and each assembly contains 91 fuel pins yielding a 514 kg heavy metal load for the fresh core [30].

The central hexagonal position is let free to insert into spallation module and should be of limited dimensions (about 10 cm) in order to keep many locations available in the core to install experiments where the neutron flux is sufficiently high.)

The main design modifications due to the critical operation mode are, first of all, the introduction of the primary and secondary shutdown systems.

Additional analyses have been carried out to find the most suitable design for the absorbers. Because of diversification two different types of absorber sets are foreseen, one to be inserted from the bottom and driven by buoyancy in LBE, and a gravity driven absorber set inserted from the top in two dedicated gas (He) channels positioned in the centre-middle zone of the layout. For the gravity driven absorber rod in gas, there is back-up option based on a gravity driven rod in LBE with forced injection as in BR2 or BWRs. This choice allows to reduce the number of “empty” channels in the core and to minimize the void effect [30]. The (gravity driven) secondary set is used only for shutdown. Otherwise the primary set, made by six buoyancy driven absorber placed at the core boundary, has the double function of shutdown and control.

The thermal power is removed by 4 heat exchangers (counter-current flow shell and tube type) with water flowing inside the tubes and LBE flowing in shell side. Figure 4 shows a conceptual layout.

![Figure 4. Concept of the primary heat exchangers.](image-url)
2.5.2 Thermal hydraulic characterization in normal operating conditions

The nominal LBE flow rate passing through the core is around 5000 kg/s, which allows an average core temperature difference of about 140 °C. In this first simplified characterization we do not take into account the by-pass flow along the core foreseen in actual design and consequently the temperature difference through the core will be slightly different from the lower plenum and upper plenum temperature difference.

Looking at Figure 3, the LBE is pumped from the lower plenum through the core by two pumps put inside the vessel. The pressure drop through the core due to the friction is fixed by design at a value of 2.1 bar. After the core the LBE enters in the core barrel, a big tube getting up until the gas plenum. In the barrel wall there are holes, so the hot liquid metal gets gradually to the upper plenum region. MYRRHA is characterized by two free surfaces levels: the first one is the upper plenum level and the second one is connected to the lower plenum. The two free surfaces are in contact with the common gas plenum. The level difference between the two free surfaces is mainly due to the pressure drop occurring in the core region. From the upper plenum the liquid metal reaches the 4 heat exchangers which remove the thermal power. The difference in height between heat exchangers and the core mid planes is fixed to a value of 1 m: this value should allow natural circulation conditions in incidental transient with decay heat removal [31]. In the following Table 1, we summarize the main parameters of the present MYRRHA design: starting from these values a first thermal hydraulic characterization has been done.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Vessel height [m]</td>
<td>12.6</td>
</tr>
<tr>
<td>Vessel diameter [m]</td>
<td>7.56</td>
</tr>
<tr>
<td>Total area fuel assemblies outlet core [m²]</td>
<td>0.42</td>
</tr>
<tr>
<td>Volume [m³]</td>
<td>420</td>
</tr>
<tr>
<td>Fuel assembly outlet velocity [m/s]</td>
<td>0.97</td>
</tr>
<tr>
<td>Flow rate [kg/s]</td>
<td>4957</td>
</tr>
<tr>
<td>Core pressure drops [bar]</td>
<td>2.1</td>
</tr>
<tr>
<td>Core power [MW]</td>
<td>100</td>
</tr>
<tr>
<td>Core temperature difference [°C]</td>
<td>140</td>
</tr>
<tr>
<td>Reynolds Number (Calculated at the pump outlet)</td>
<td>$5.36 \cdot 10^6$</td>
</tr>
</tbody>
</table>

Table 1. Thermal-hydraulic characterization of MYRRHA.
Since a detailed MYRRHA characterization is beyond the scope of this work, we will focus only on the main design parameters that we will need for a preliminary reactor scaling analysis.

### 2.5.3 Thermal-hydraulic characterization in natural circulation conditions

A first prediction of MYRRHA natural circulation with decay heat removal is needed to perform the scaling analysis. Contrary to the case of normal operating conditions, preliminary data on incidental transients in the real plant are not yet available for the latest geometry. As the first step of the analysis, the MYRRHA natural circulation capability would be examined under a steady-state of full loss of primary flow with decay heat power. To estimate the MYRRHA natural circulation capability we used in a first instance a simplified analytic solution of an equivalent loop configuration. For the detailed developments we refer to the Ref. [32].

For steady state single-phase natural circulation the momentum equation can be reduced to:

\[
\Delta p_f = \Delta p_R
\]

where \( \Delta p_R \) represents the buoyancy contribution between regions with different values of density:

\[
\Delta p_R = \beta \rho_0 \Delta T \Delta L
\]

and \( \rho_0 \) is the reference density at the reference temperature \( T_0 \), while \( \beta \) is the thermal expansion coefficient. The physical interpretation of this relation is that the buoyancy head is equal to the difference between the maximum coolant density and the minimum coolant density along the loop times the difference in elevation between the thermal centre of heat extraction and the thermal center of heat addition. This contribution should be equal to the frictional losses \( \Delta p_f \). Frictional losses may be written in the form:

\[
\Delta p_f = \Delta p_{core} + \Delta p_{E,X} + \Delta p_{barrel} = C_R \frac{m^2}{2 \rho_l}
\]

where \( C_R \) is the hydraulic resistance coefficient and \( m \) the flow rate in the loop.

\( C_R \) is depending on the flow rate: we can write \( C_R = R(m)^{-n} \) where \( R \) is the proportionality constant which can also include form losses. For highly turbulent flow and smooth pipe \( n=0.2 \) [32]. Anyway in this case it is more correct to consider an almost quadratic (\( n=0 \)) \( \Delta p_f \).
dependence with the flow rate, because we are dealing with rough pipes in fully turbulent flow. Therefore:

\[ \Delta p_f = \frac{1}{2} R \left( \frac{m}{\rho} \right)^{2-n} \]  

(5)

We first approximate the hydraulic resistance proportionality constant \( R \) by considering the normal operating conditions. In first instance we estimate that the pressure drop in the loop can be considered about 20% \([14]\) of the value of the pressure drop in the core. Consequently: \( \Delta p_f = 2.1 \cdot 1.2 = 2.52 \) bar, considering a flow rate through the core that is 4957 kg/s.

We consider also small variations of density; so, \( \rho_0 = \rho_l \). The calculation gives a value of \( R=211 \). Substituting Eq. (5) and Eq. (3) in Eq. (2) we obtain the following relation for a given heating power \( \dot{Q}_h \) in natural circulation:

\[ m = \left( \frac{2 \beta \dot{Q}_h}{c_p R} \frac{\Delta L}{R} \rho_0^2 \right)^{1/(3-n)} \]  

(6)

The values which will be used in the Eq. (6) have been evaluated at 8000 s after the core shutdown (after this period the decay heat removal can be considered almost stationary):

- core power after 8000 s: 1.2 MW (1.2% of the total heat power)
- \( \Delta L = 1 \) m (height difference between core and heat exchanger mid planes).

This analysis gives a value of flow rate in natural circulation of 322 kg/s. Consequently, we can get the main parameters of MYRRHA natural circulation:

- Flow rate: 220 kg/s.
- Core power after 8000 s: 1.2 MW (1.2% of the total heat power).
- Core temperature difference: 37.8 °C.

\[ \omega = \frac{\dot{m}}{\rho A} = \frac{220}{10292 \cdot 0.4382} = 0.048 \text{ m/s} \quad (\text{fuel assemblies outlet}). \]

It should be clear that in an actual reactor system the temperature increase in the core may not be linear, and the temperature decrease in the heat exchanger may be close to exponential. Thus, the solution of the exact equations in reactor system may have to be achieved numerically. Zvirin et al. [33] found that the difference between the exact solution and the
linear temperature models is small, the deviation is in the order of 5 % for $m$ and $\Delta T_\mu$ for the conditions addressed by Zvirin.
Chapter 3. Scaling approach for liquid metal pool thermal hydraulics

3.1 Generality

The importance of similarity laws and scaling criteria has been shown extensively in research. Scaling methods are necessary when the study of phenomena cannot be accomplished using a prototype model. This is often the case for large systems where full-scale experiments would be dangerous and costly. It is not feasible (or cost prohibitive) to perform meaningful experiments at full scale and the ability of numerical tools designed to simulate the performance of nuclear reactors can be proven only at a reduced scale [34].

The scaling laws for forced convection single phase flow are well established and modeling with these criteria is common practice. The natural circulation single phase problem has been investigated by Heisler (1981) and Singer (1982) [34]. The similarity analysis in this case is very complex since there is a coupling of driving forces and heat transfer processes. Two-phase flow similarity criteria were developed also by Zuber (1970) who created a hierarchy among scaling factors and a scaling design or requirements to eliminate the arbitrariness in scaling and providing a quantitative estimate of the importance of the scaling factors [35].

Remarkable contributions related to the scaling issue come from the scaling methodology developed by Ishii and Kataoka (1984), who derived similarity laws for single phase and two-phase convection. The bases of these scaling criteria are the conservation principles and the constitutive laws [36].

The Ishii scaling method [34] consists of three levels of scaling analysis, namely the integral system scaling, control volume scaling and local phenomena scaling. The first two levels correspond to the top-down scaling and the third level represents the bottom-up scaling. The prototype system consists of multiple inter-connected components. The first level of the scaling method focuses on the development of scaling criteria for each component using the response function scaling. It is assumed that each component can be mathematically described as a one dimensional system. A combination of the single phase formulation and the drift-flux two phase flow combination is used to express the conservation principles of mass, momentum and energy [34]. First, the system conservation equations are solved under a
transient condition using a linear small perturbation analysis. The solution yields various transfer functions between variables, for example between the inlet flow, void fraction, enthalpy and pressure. It is noted that these transfer functions describe the system dynamic responses. The similarity criteria are developed by non-dimensionalizing the transfer functions and then by identifying the conditions to make the non-dimensional transfer functions to be identical between the prototype and an integral test facility. Therefore, it can be said that the scaling method gives the dynamic scaling of a whole component.

The second level of the scaling analysis focuses on the mass and energy inventory of each component and the inter-component mass and energy transfers. This is accomplished by introducing a scaling method based on the control volume balance equations of mass, momentum and energy. The first and second level scaling analysis are, therefore, based on the conservation principles of mass, momentum and energy. Together they ensure that the dynamic responses of each component as well as the dynamic responses of the inter-component transfers are simulated. These two levels of scaling analysis yield the bulk of the information necessary to develop the scientific design of a test facility [34].

However, these two levels of analysis are not sufficient to guarantee the development of a well-scaled facility design. This is due to the fact that, in two-phase flow various local phenomena have their own internal length scales and the micro-scale physical phenomena that affect various transfer mechanism may not be fully represented by a simple one-dimensional drift-flux formulation. Therefore, it is necessary to evaluate the key local phenomena and various constitutive relations in terms of scaling. The third level of scaling analysis focuses on the various local phenomena, constitutive laws, and their impact on the overall scaling strategy. The scaling analysis at this level is typically carried out for each phenomenon separately by considering it as a simple rate process or as transition criterion for bifurcation. The basic question addressed with the third level is: “is there the possibility to affirm that, either experimental data or code calculation results, are able to characterize the value of a thermal-hydraulic parameter valid ‘locally’ with no or ‘small’ uncertainty?”[37]. The answer is generally no, and the third level gives the potential scaling distortions and a possible way to minimize such distortion [34].

A comprehensive scaling analysis in nuclear reactor technology shall address and actually addresses each one of the three sectors. The Ishii scaling allows the solution of issues like high cost of a facility and large impact of heat stored in passive structures upon the transient evolution. However, the ‘Ishii scaling’ needs the time-preserving scaling for validation and, adopted alone, would have not created the current level of confidence in the
simulation of complex phenomena. In relation to the main scaling approaches for Integral Test design, identified as ‘time-preserving’ and ‘Ishii-scaling’, the availability of a myriad of scaling parameters may reveal useless. This is because of the difficulty in defining the quantitative acceptability of a scaling distortion or in quantifying the impact of a distortion upon a scenario.

One of the most important requirements in a liquid metal fast reactor is the decay heat removal after reactor shut-down. Heat removal through natural circulation is reliable because of the passivity. In order to adopt a natural circulation system, one must have an accurate prediction tool which enables to quantitatively estimate single phase natural circulation.

One option for prediction is a scaled-model experiment with a suitable thermal hydraulic similarity, by which an experiment is quantitatively related to an actual plant. The other is a computational method, by which a set of equations describing an actual plant is numerically solved. In many previous works, natural circulation was predicted with the latter method, using computer codes verified by a scaled model. This is also the target of this work. Looking at the predictability of the transient performance of a nuclear reactor where a numerical code is needed, several closure equations are part of the code. Now, the paradox: closure equations in order to be qualified must be derived in the conditions of Steady State and Fully Developed (SS and FD) flow; however, they are unavoidably used in transient (as opposite to SS) and non-developed (as opposite of FD) flow conditions. Thus, even though the (closure) equations are qualified, no scaling-up outside the range of qualification is justified. The argument in the last sentence can be used within the present context, i.e. the difficulty to confirm the validity of scaling laws [37].

3.2 Non-dimensional analysis

The selection of the scaling parameters for the model experiment is based on the requirement that the overall behavior in the prototype plant is preserved and the major thermal-hydraulics phenomena reproduced while considering economics like initial and operational cost [9].

Since E-SCAPE focuses on upper and lower plenum behavior and it is practically impossible to manufacture reduced scale models for complicated components such as fuel assemblies or tube bundles, the core and the heat exchangers will be assumed as ‘black boxes’ in which the fluid flow is unidirectional and subject to uniform resistance acceleration \( f_{\text{core}} \) and \( f_{\text{HX}} \) uniform volumetric heating or cooling, \( H_{\text{core}} \) and \( H_{\text{HX}} \) [9]. With these assumptions
and the Boussinesq fluid approximation, Eguchi [38] derives a set of basic equations describing the motion and temperature of the fluid:

\[ \begin{align*}
\bullet & \quad u_{i,j} = 0 \quad \text{in } (\Omega) \quad (7) \\
\bullet & \quad u_{i,j} + u_j u_i = \nu u_{i,j,j} + \beta(T - T_0) g \gamma_{i3} - p_{,i} / \rho - f^\text{core} \sum (\Omega^\text{core}) \delta_{i3} + f^\text{HX} (\Omega^\text{HX}) \delta_{i3} \quad \text{in } (\Omega) \quad (8) \\
\bullet & \quad T_{,j} + u_j T_{,j} = \alpha T_{,j,j} + H^\text{core} \sum (\Omega^\text{core}) / \rho C - H^\text{HX} \sum (\Omega^\text{HX}) / \rho C \quad \text{in } (\Omega) \quad (9)
\end{align*} \]

where Einstein’s summation convention is used for repeated i and j and the symbols of \( \sum (\Omega^\text{core}), \sum (\Omega^\text{HX}) \) represent “hat functions”, whose values are unity within core region \( \Omega^\text{core} \) and HX region \( \Omega^\text{HX} \) respectively, being zero outside these regions. \( H \) is the heating or cooling rate in a “black box” component.

In order to derive non-dimensional numbers, velocity, pressure, temperature and coordinates are converted in dimensionless form with a representative velocity \( U \), a representative temperature difference \( \Delta T \) and a representative length \( L \) obtaining the non-dimensional form of governing equations.

From the non-dimensional form of these equations, boundary and initial conditions, six non-dimensional quantities can be derived. Eguchi [38] classifies them into three groups according to the properties and significance:

**1st Group**

- Richardson
  \[ Ri = \frac{g \beta \Delta T L}{U^2} = \frac{\text{Buoyancy}}{\text{Inertia}} \]

- Euler or Friction number
  \[ Eu = \left( \frac{f L}{D} + K \right) = \frac{\text{Friction}}{\text{Inertia}} \]

- Heat source number
  \[ H.S = \frac{q L}{\rho c_p U \Delta T} = \frac{\text{Heat source}}{\text{Axial energy change}} \]

**2nd Group**

- Stanton number
  \[ St = \frac{h}{\rho c_p U} = \frac{\text{Heat transfer at wall}}{\text{heat transfer}} \]

**3rd Group**

- Reynolds
  \[ Re = \frac{\rho U L}{\mu} = \frac{\text{Inertia}}{\text{Viscosity}} \]

- Péclet
  \[ Pe = \frac{\rho c_p U L}{k} = \frac{\text{Convection}}{\text{Conduction}} \]
The numbers in group 1 are from source terms in the momentum and energy equations and they have direct impact on the solutions \( u, p, \) and \( T \). Priority must be given to these numbers when selecting similarity conditions [38].

The *Richardson number* is only relevant if temperature differences are present which lead to a significant contribution by buoyancy [34, 39].

The preservation of the *Euler number* is fulfilled if pressure loss coefficients in the “black boxes” are preserved [34,38]:

\[
\sum_i (Eu_i)_R = 1
\]

where the subscript \( i \), designates the particular component and \( R \) denotes the ratio of the property of the prototype to that of the model. This assumes that pipe friction loss and singular losses associated with the loss coefficient can be interchanged without changing the overall value of the pressure loss term. So, with the addition of orifices, which provide flow restriction and increased frictional losses, a wide range of scaling conditions can be simulated. The pressure loss term can then be satisfied independently from the remaining scaling requirements [34]. This conclusion is particularly helpful for the development of the system code model because it allows to adapt the friction coefficients to get the desired operating conditions.

The *heat source* number is important for the temperature in the solid and also for the overall energy balance of the system. The term *heat source* number is taken from Ishii’s reference paper on the three-level scaling approach [34].

The *Stanton* number (group 2) used in this work is derived from Eguchi’s boundary condition number using the Péclet number [9, 38]. This number is related to boundary conditions (heat losses) and is negligible if heat flux through the fluid boundary is negligibly small in both experiment and actual plant.

The numbers in group 3 are the reciprocals of the coefficients of second derivatives (diffusion terms) in the momentum and energy equations. They have rather local and less significant effect on the solution, partly because their effect is limited to areas where the value of the second derivates of \( u \) and \( T \) is large enough and mainly because molecular diffusion is often made negligible by the overwhelming effect of turbulent diffusion. For liquid metals with a high thermal conductivity, the molecular diffusion might be of larger importance [9].

When flow patterns near free surfaces are of interest and/or flow is driven by gravity, the *Froude number* should be preserved:
\[ Fr = \frac{U^2}{gL} = \frac{\text{Inertia}}{\text{Gravity}} \]

This number thus scales the gravitational head against the inertia term.

Residence times in the jet region, \( \tau = \frac{V_{\text{in}}}{Q_m} \), are calculated as the ratio of the volume occupied by the jets and the inlet volume flow rate to the jet [40]. The product of the residence time and the specific or characteristic frequency gives the specific or characteristic time ratio \( \Pi \). This time ratio is the change in mass, momentum, energy or species during the fluid residence in a volume [40]. When local phenomena (e.g., chemical reactions) are of interest, time conservation is important.

The flow in the lower plenum is mainly dictated by the two pump jets [9]. Consequently, particularly important is the study of forced jets. Peterson [9, 40] states that for forced jets, if the aspect ratio between jet length and inlet diameter is preserved, similar mixing and entrainment can be expected with full scale and reduced scale jets. For buoyant plumes, if also the Richardson number is preserved, the transition height from jet to plume behaviour scales properly [40]. The quantities above are the main non-dimensional numbers which characterize E-SCAPE phenomena.

For complete similarity all these non-dimensional numbers should be equal in the experiment and in the actual plant. In principle, it was derived a scaling strategy which preserves all the relevant non-dimensional quantities in the three groups mentioned above while using prototypic fluid. From the preservation of the Reynolds and the Péclet number, one derives:

\[ \text{Re}_{\text{ESCAPE}} = \text{Re}_{\text{MYRRHA}} \rightarrow \rho U_E L_E = \rho U_M L_M \rightarrow \frac{U_E}{U_M} \approx L_M \rightarrow U_R \approx \frac{1}{L_R} \]

where the subscript \( R \) denotes the ratio between quantities and the subscripts \( E, M \) indicate E-SCAPE and MYRRHA respectively.

From Richardson number, the Boussinesq fluid approximation, and remembering the above relation for velocity, one obtains:

\[ Ri_E = Ri_M \rightarrow \frac{g \beta \Delta T E L_E}{U_E^2} = \frac{g \beta \Delta T M L_M}{U_M^2} \rightarrow \frac{\Delta T E L_E}{\Delta T_M L_M} \approx \frac{U_E^2}{U_M^2} \rightarrow \Delta T R \cdot L_R \approx \frac{1}{L_R^2} \rightarrow \Delta T R \approx \frac{1}{L_R^3} \]

Keeping the above relations and preserving also the heat source number we can obtain:

\[ (H.S)_E = (H.S)_M \rightarrow \frac{q_E L_E}{\rho c_p U_E \Delta T E} = \frac{q_M L_M}{\rho c_p U_M \Delta T M} \rightarrow \frac{q_E}{q_M} \approx \frac{L_M \Delta T E U_E}{L_E \Delta T M U_M} \rightarrow q \approx \frac{1}{L_R} \cdot \frac{1}{L_R} \cdot \frac{1}{L_R} \rightarrow q \approx \frac{1}{L_R^3} \]
As far as the Euler number is concerned, we explained above that the pressure loss term can be satisfied independently of the remaining scaling requirements.

The Stanton number defines the heat transfer coefficient at the boundaries and can be disregarded by considering negligible the power losses (as it was assumed in this work). It is generally determined by Reynolds and Péclet numbers.

With this approach it seems possible to preserve all the six quantities in the three groups and obtain a facility completely simile to the real plant in both forced and natural circulation conditions.

However, such kind of approach will lead to values for temperature changes and power density that cannot be realized in practice. Another approach should therefore be studied for the E-SCAPE design.

### 3.3 Scaling approach used for E-SCAPE

Because of the large number of processes and parameters, it is not possible to design and operate a test facility granting complete similarity between downscaled experiments and full scale plant operating conditions. Thus, the non dimensional analysis shown before leads, as said, to values for temperature changes and power density that cannot be realized in practice.

Consequently, based on the important phenomena and processes identified in the previous sections, the optimum similarity criteria should be identified, together with the associated scaling rationales developed for designing and operating the facility. E-SCAPE focuses in a first instance on forced convection flow. Below we will show an overview of different scaling approaches for forced convection. Then, we will investigate the possibility to study also MYRRHA natural circulation using E-SCAPE.

#### 3.3.1 Scaling approach for forced convection flow

E-SCAPE, as said above, focuses in a first instance on forced convection flow. In this case, buoyancy is not important and the Euler and heat source numbers are the driving non dimensional parameters, for momentum and energy conservation.

Again, since pressure loss coefficients are combinations of distributed friction and form loss terms, this requirement is easily fulfilled by adapting form loss coefficients to compensate for changes in friction loss terms, as explained above.
The *heat source* number provides a linear relation between the power density in a “black-box” heater and the temperature difference across it (without specifying details of phenomena inside the component). Specifying one determines the other:

\[
(H.S)_{E} = (H.S)_{M} \Rightarrow \frac{q_{E}^{\nu} L_{E}}{\rho c_{p} U_{E} \Delta T_{E}} = \frac{q_{M}^{\nu} L_{M}}{\rho c_{p} U_{M} \Delta T_{M}} \Rightarrow \frac{q_{E}^{\nu}}{q_{M}^{\nu}} = \frac{L_{M} \Delta T_{E} U_{E}}{L_{E} \Delta T_{M} U_{M}} \Rightarrow q_{R}^{\nu} \approx \frac{U_{E}^{\nu} L_{E}}{L_{R}^{\nu} \Delta T_{R}}
\]

The relation between velocity and length scaling should thus be determined on the basis of other dimensionless numbers:

- If time conservation is important, velocity should scale as length (e.g., when chemical reactions are important) from the definition of residence time:

  \[
  \tau_{R} = \frac{\tau_{E}}{\tau_{M}} \Rightarrow U_{R} \approx L_{R}
  \]

- If free surfaces appearance is important, Froude dictates that velocity scales as the square root of height. In principle, a different scaling can be chosen for heights and horizontal lengths. This is not considered for E-SCAPE since the preservation of the aspect ratio is important for jet behavior:

  \[
  Fr_{E} = Fr_{M} \Rightarrow \frac{U_{M}^{2}}{g L_{M}} = \frac{U_{E}^{2}}{g L_{E}} \Rightarrow \frac{U_{E}^{2}}{L_{M}} = \frac{L_{E}}{U_{M}} \Rightarrow U_{R} = \sqrt{L_{R}}
  \]

  This criterion is acceptable as long as the Reynolds numbers are high enough so that turbulent diffusion dominates over molecular effects.

- If it is not true or if local turbulence characteristics are important, velocity should scale according to Reynolds preservation:

  \[
  Re_{ESCAPE} = Re_{MYRRHA} \Rightarrow \frac{\rho U_{E} L_{E}}{\mu} \Rightarrow \frac{\rho U_{M} L_{M}}{\mu} \Rightarrow \frac{U_{E}}{U_{M}} \Rightarrow \frac{L_{M}}{L_{E}} \Rightarrow U_{R} \approx \frac{1}{L_{R}}
  \]

Depending on the focus of the experimental program (time preservation, surface appearance, turbulence), a different flow velocity-length relation can be chosen using the relations above. In the design of the E-SCAPE facility, a wide span in flow rates is foreseen. From a technical viewpoint, however, it is not feasible to have flow velocity above 2 m/s in the experiment. Since 2 m/s is about the velocity of the jet of pump in the real plant, downscaling on the basis
of the *Reynolds* number would immediately result in too large velocities. Considering a scaling factor of 1/6 it results in velocities of about 12 m/s in E-SCAPE.

It has been therefore decided not to consider the Reynolds scaling criterion but to study a “maximum velocity scaling” with jet velocities of the scaled facility similar to those in the full-size case [9].

The working fluid is selected to be the prototype fluid (LBE), so that prototypical temperature conditions are kept and experience with LBE technology will be gained. After the choice of the scaling criterion for selecting velocity, it is possible to obtain the other values of interest for the design.

For the importance of free surface appearance and jet behavior, the first scaling approach selected in this work for calculating velocity is to preserve the *Froude* number, representing the *ratio* between inertia and gravity:

\[ Fr = \frac{U^2}{gL} = \frac{\text{Inertia}}{\text{Gravity}} \]

The main parameters are then derived according to the Froude scaling, with a length scaling factor given by \( L_R = 1/6.3 \) (*length ratio*). Again, in the following relationships, the subscript *M* is for MYRRHA and *E* is for E-SCAPE.

- The *velocity ratio* \( U_R \) is given by:
  \[ Fr_E = Fr_M \Rightarrow \frac{U_M^2}{gL_M} = \frac{U_E^2}{gL_E} \Rightarrow \frac{U_E^2}{U_M^2} = \frac{L_E}{L_M} \Rightarrow U_R \approx \sqrt{L_R} \]

- The *flow rate ratio* \( F_R \) is given by:
  \[ F_R = \rho U_R A_R = \rho \sqrt{L_R} \cdot L_R^2 \Rightarrow F_R^2 \approx L_R \cdot L_R^4 \Rightarrow F_R \approx \sqrt{L_R}^5 \]

- The *drop of pressure ratio* \( \Delta P_R \) is obtained remembering the definition of Euler number and writing the pressure drop ratio as follows:
  \[ \frac{\Delta P_E}{\Delta P_M} = \frac{Eu_E}{Eu_M} \cdot \frac{\rho U_E^2}{\rho U_M^2} \]

Preserving the value of the *Euler* number in the plant and the facility \( \left( \frac{Eu_E}{Eu_M} \right) = 1 \) and remembering the relation between *velocity ratio* and *length ratio* \( (U = \sqrt{L}) \) we obtain:

\[ \frac{\Delta P_E}{\Delta P_M} \approx \frac{L_E}{L_M} \Rightarrow \Delta P_R \approx L_R \]
Substituting in the relationship of core volumetric power as a function of temperature difference provided by the heat source number, the relation provided for $U_R$ by the criterion of preserving the Froude number, we obtain that:

$$q''_{R} = \frac{U_R}{L_R} \Delta T_R = \frac{\sqrt{L_R}}{L_R} \cdot \Delta T_R = \frac{1}{\sqrt{L_R}} \cdot \Delta T_R$$

and consequently we obtain a relation also between $Q$ and $\Delta T$:

$$Q_R = q_R \cdot V_R = \frac{L_R^3}{\sqrt{L_R}} \cdot \Delta T_R = \sqrt{L_R^3} \cdot \Delta T_R$$

We can obtain the same result also using the relation found for the flow rate ratio:

$$Q_R \approx F_R \Delta T_R \approx \sqrt{L_R^3} \cdot \Delta T_R$$

- The Reynolds number ratio is found to be:

$$Re = \frac{Re_E}{Re_M} = \frac{U_E L_E}{U_M L_M} = U_R \cdot L_R = \sqrt{L_R} \cdot L_R = \sqrt{L_R^3}$$

- The Péclet number ratio:

$$Pe = \frac{Pe_E}{Pe_M} = \frac{U_E L_E}{U_M L_M} = U_R \cdot L_R = \sqrt{L_R} \cdot L_R = \sqrt{L_R^3}$$

as the Prandtl number is the same in the two cases.

- The time ratio:

$$t = \frac{t_E}{t_M} = \frac{V_E / \dot{Q}_E}{V_M / \dot{Q}_M} = \frac{L_R^3}{\sqrt{L_R^5}} = \sqrt{L}$$

The main idea is thus to obtain an exactly scaled copy of MYRRHA, in which all the lengths are scaled identically in all the directions (isotropic scaling). The choice of the length scaling factor is based on the principle of optimizing all the above requirements while considering economics. CFD codes calculations for different scaling approaches and different scaling factors 1/5 and 1/8 have been performed by SCK•CEN. Reynolds scaling, as shown above, was found to be impractical and therefore it was discarded. The other scaling strategy lead to flow patterns that are close to the full scale situation for both scaling factors [9].
Economic considerations push towards smaller scales; practical considerations such as power density and instrumentation access demand for larger scale. For the E-SCAPE facility, a vessel diameter of 1.2 m was considered optimal in this respect. This corresponds to a geometrical scaling factor of 1/6.3. It is therefore important to evaluate if with this setting the Reynolds number is still large enough to obtain turbulent flow conditions. In the following plot (Figure 5) it is verified that with the 1/6.3 length scaling factor proposed for ESCAPE, the Reynolds number is large enough so that turbulent diffusion still dominates.

When the length scaling factor $L_R$ is 1:

$$\text{Re}_E \approx \text{Re}_M \approx \frac{\rho U_M L_M}{\mu} \approx 5.36 \cdot 10^6$$

where $U_M$ is the velocity at the outlet of the pump and $L_M$ the diameter of the pump in MHYRRA.

When the scaling factor $L_R$ is 1/6.3 we obtain: $\text{Re}_E \approx 5.36 \cdot 10^6 \cdot L_R^{1.5} \approx 338659$. This value is still acceptable to keep turbulent conditions in forced convection, but we will have to pay attention if a similar condition can be obtained also in natural circulation.

In Figure 5 we can see trend of the Reynolds number as a function of the length scaling factor.

![Figure 5. Length scaling factor in function of E-SCAPE Reynolds.](image)
With the same approach used for the Froude number preservation, it is possible to calculate the main scaling factors using different scaling approaches like Reynolds Number, Time and Maximum Velocity preservation. The scaling parameters for the four different scaling approaches that were identified at the beginning for E-SCAPE are listed in Table 2.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Froude scaling factors (Ratio)</th>
<th>Reynolds scaling factors (Ratio)</th>
<th>Time scaling factors (Ratio)</th>
<th>Max. velocity scaling factors (Ratio)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Length</td>
<td>$L_R$</td>
<td>$L_R$</td>
<td>$L_R$</td>
<td>$L_R$</td>
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<td>Power/vol.</td>
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<td>$\Delta T_R / L_R^2$</td>
<td>$\Delta T_R$</td>
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<tr>
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</tr>
</tbody>
</table>

Table 2. Scaling factors in forced convection for a scaling factor $L_R = 1/6.3$ with different scaling criteria.

With these relations and using the latest available configuration assumed for MYRRHA, we can scale the reactor dimensions and parameters to get a first ideal configuration of the test facility. Reynolds number cannot be kept and the time ratio has not been studied in this work.
Consequently, we show the E-SCAPE operating conditions only for Froude number preservation and max. velocity preservation scaling approaches.

In MYRRHA we have:

\[
\dot{Q} = 100\text{MW}; \Delta T = 140^\circ\text{C} \rightarrow \dot{m}_{\text{MYRRHAcore}} = 4957\text{kg/s}
\]

Consequently using the relations of Table 2, we can get the flow rate in the E-SCAPE core using two alternative preservation criteria.

\[
\dot{m}_E \approx \sqrt{L^2} \cdot \dot{m}_M \approx 49.75\text{ kg/s} \quad \text{Froude}
\]

\[
\dot{m}_E \approx L^2 \cdot \dot{m}_M \approx 124.9\text{ kg/s} \quad \text{Max velocity}
\]

In E-SCAPE technical reasons limit the total core heating to a value of about 100 kW depending on the actual size of the core “black box”. This value will be not correctly scaled to the core power during operation of the MYRRHA reactor. It means that it will not be possible to keep the same \( \Delta T \) in the plant and the facility. Fixing the power ratio \( Q_R \) to a value of \( 10^{-3} \) (100 MW is the MYRRHA power, while 100 kW the E-SCAPE power) we can obtain the E-SCAPE temperature difference:

**Froude preserving criterion:**

\[
\dot{Q}_R \approx 10^{-3} \approx \sqrt{L^2} \cdot \Delta T_R \rightarrow \Delta T_E \approx \frac{10^{-3}}{\sqrt{L^2}} \cdot \Delta T_M \approx 13.8^\circ\text{C}
\]

we can confirm the validity of the relation with a simple energy balance:

\[
\dot{Q}_E = m_{E \text{c}} \rho \Delta T_E \rightarrow \Delta T_E = 13.8^\circ\text{C}
\]

**Max. velocity preserving criterion:**

\[
\dot{Q}_R \approx 10^{-3} \approx L^2 \cdot \Delta T_R \rightarrow \Delta T_E \approx \frac{10^{-3}}{L^2} \cdot \Delta T_M \approx 5.55^\circ\text{C}
\]

Looking at the temperature difference values obtained above, it is possible to see that we will get a scaled temperature pattern between plant and prototype. The pressure drops across the MYRRHA core are fixed at the moment to a value of 2.1 bar. We can obtain the E-SCAPE value:

**Froude preserving criterion:**

\[
\Delta P_E \approx \Delta P_M \cdot L \approx 2.1 \cdot \frac{1}{6.3} \approx 0.33\text{ bar}
\]

**Max. velocity preserving criterion:**

\[
\Delta P_E = \Delta P_M = 2.1\text{ bar}
\]
and the same approach (following Table 2 relations) is applied to the other values of interest for the analysis, which are shown in Table 3. The results of the scaling analysis in forced convection for E-SCAPE shown in Table 3, are calculated according to the Froude driving number and max. velocity driving number.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value MYRRHA</th>
<th>Scaling factors</th>
<th>Value E-SCAPE</th>
<th>Scaling factors</th>
<th>Value E-SCAPE</th>
</tr>
</thead>
<tbody>
<tr>
<td>Vessel height [m]</td>
<td>12.6</td>
<td>1/6.3</td>
<td>2</td>
<td>1/6.3</td>
<td>2</td>
</tr>
<tr>
<td>Vessel diameter [m]</td>
<td>7.56</td>
<td>1/6.3</td>
<td>1.2</td>
<td>1/6.3</td>
<td>1.2</td>
</tr>
<tr>
<td>Total area fuel assemblies outlet core [m²]</td>
<td>0.42</td>
<td>1/39.7</td>
<td>0.010</td>
<td>1/39.7</td>
<td>0.010</td>
</tr>
<tr>
<td>Volume [m³]</td>
<td>420</td>
<td>1/250</td>
<td>1.68</td>
<td>1/250</td>
<td>1.68</td>
</tr>
<tr>
<td>Fuel assembly outlet velocity [m/s]</td>
<td>0.97</td>
<td>1/2.5</td>
<td>0.38</td>
<td>1</td>
<td>0.973</td>
</tr>
<tr>
<td>Flow rate [kg/s]</td>
<td>4957</td>
<td>1/99.62</td>
<td>50</td>
<td>1/39.6</td>
<td>125</td>
</tr>
<tr>
<td>Core pressure drops [bar]</td>
<td>2.1</td>
<td>1/6.3</td>
<td>0.33</td>
<td>1</td>
<td>2.1</td>
</tr>
<tr>
<td>Core power [kW]</td>
<td>100000</td>
<td>1/1000</td>
<td>100</td>
<td>1/1000</td>
<td>100</td>
</tr>
<tr>
<td>Temperature difference [°C]</td>
<td>140</td>
<td>1/10.15</td>
<td>13.8</td>
<td>1/25.9</td>
<td>5.55</td>
</tr>
<tr>
<td>Reynolds (Calculated at the pump outlet)</td>
<td>5.36 (\cdot) (10^6)</td>
<td>1/15.81</td>
<td>338659.394</td>
<td>1/6.3</td>
<td>8.5 (\cdot) (10^5)</td>
</tr>
<tr>
<td>((Re)_{ratio})</td>
<td></td>
<td>0.063193295</td>
<td></td>
<td>0.15</td>
<td></td>
</tr>
<tr>
<td>((Fr)_{ratio})</td>
<td></td>
<td>1</td>
<td></td>
<td>6.3</td>
<td></td>
</tr>
<tr>
<td>((Eu)_{ratio})</td>
<td></td>
<td>1</td>
<td></td>
<td>1</td>
<td></td>
</tr>
</tbody>
</table>

Table 3. E-SCAPE design parameters from MYRRHA data in forced convection.

The non dimensional numbers, in both the plant and the facility, have been calculated in the following way:

\[
Fr = \frac{U^2}{gL} = \frac{Inertia}{Gravity}
\]

- \(U\) = velocity on the outlet region of the pump
- \(L\) = Height difference between the two free surface levels
\[ \text{Re} = \frac{\rho UL}{\mu} = \frac{\text{Inertia}}{\text{Viscous forces}} \]

- U = velocity on the outlet region of the pump
- L = Pump diameter

Looking at Table 3, the max. velocity scaling approach will provide a difference in the height between free surfaces of about 2 m, as long as the pressure drop through the core will be 2.1 bar. This value of free surface level difference is too large. It justified therefore to adopt the Froude scaling approach for the facility design and avoid in a first instance also max. velocity scaling approach.

A first analysis of the facility behavior can be performed.

The geometrical parameters of the real plant will be adapted according to engineering feasibility. For example the heat exchangers, as we will see in chapter 4, in the first design configuration is put outside of the vessel: preserving the aspect ratio between jet length and inlet diameter it should not be an important issue for forced convection flow, but we will see that it is not the case for natural circulation conditions.

A RELAP 5 model of E-SCAPE in forced convection flow for the actual design, with the heat exchangers outside the vessel, was set up for the validation of the Froude scaling approach.

### 3.3.2 Natural circulation flow

With the 100 kW depending on the actual size of the core “black box” is not possible to preserve temperature differences and the power density during normal operating conditions in the actual plant and the facility but, depending on the scaling strategy and on the scaling factor, this value of 100 kW will be greater (for Froude, Richardson, time preservation) or less (for Reynolds and maximum velocity preservation) than the scaled value of the core decay heat when the temperature difference in natural circulation is preserved. In this case, it will be possible to maintain the value of the \( \Delta T \). This approach allows to study natural convection during decay heat removal.

When buoyancy is important, the \textit{Richardson number} provides a relation between velocity, length and temperature difference. Power density (or equivalently temperature difference), length and velocity scales can no longer be chosen independently in this case. Assuming the equality between the Richardson numbers of the plant and facility we have:
If it is chosen to preserve temperature differences, one can obtain:

\[
R_i_E = R_i_M \Rightarrow \frac{\rho \beta \Delta T_i L_E}{U_E^2} = \frac{\rho \beta \Delta T_M L_M}{U_M^2} \Rightarrow \frac{L_E}{L_M} \approx \frac{U_E^2}{U_M^2} \Rightarrow U_R \approx \sqrt{\frac{L_E}{L_M}}
\]

And then we can follow the same approach used for forced convection, but with \( \Delta T_R = 1 \):

\[
Re_R = \sqrt{\frac{L_r^3}{\mu}} \quad \rho'' \approx \frac{1}{\sqrt{L_r}} \quad Q_R \approx \sqrt{\frac{L_r^5}{\mu}} \quad Pe_R \approx \sqrt{\frac{L_r^3}{\mu}} \quad t_R \approx \sqrt{\frac{L_r}{\mu}} \quad \Delta T_R = 1
\]

Froude number preservation in this case is automatically verified; so, assuming to preserve Richardson or Froude numbers as the driving scaling numbers provides the same results. Therefore, the Froude based scaling approach is still coherent, also in natural circulation.

The Euler number and the heat source numbers should be preserved (priority must be given to these numbers when selecting similarity conditions, see paragraph 3.2). For the length scaling factor, the idea was to keep the design developed for forced convection and consequently an isotropic length scaling strategy.

In the following we try to show the validity of the Froude (Richardson) scaling approach.

For steady state single-phase natural circulation the momentum equation can be reduced to:

\[
\Delta p_f = \beta \rho_0 \Delta T_H \Delta L
\]

Considering the conditions in the experiment and in the actual plant, it should be:

\[
\frac{\Delta p_{f,E}}{\Delta p_{f,M}} = \frac{\beta \rho_0 \Delta T_E \Delta L_E}{\beta \rho_0 \Delta T_M \Delta L_M}
\]

where \( \Delta p_{f,E} \) and \( \Delta p_{f,M} \) represent the friction contribution to the pressure drop for E-SCAPE and MYRRHA respectively, \( \Delta T_E \) and \( \Delta T_M \) represent the temperature difference between the hot plenum and the cold plenum for E-SCAPE and MYRRHA respectively, while \( \Delta L_E \) and \( \Delta L_M \) represent the height difference between core and HX middle planes in the facility and actual plant respectively.

With the adopted approach, it was decided to preserve the temperature difference. Therefore, for the pressure drop ratio we have the relation \( \Delta p_R = L_R \) (see Table 4). Substituting this relation in Eq. (11) we have:

\[
\frac{\Delta p_{f,E}}{\Delta p_{f,M}} = \Delta p_R = \frac{\beta \rho_0 \Delta T_E \Delta L_E}{\beta \rho_0 \Delta T_M \Delta L_M} \Rightarrow L_R = L_R
\]
It means that both the friction contribute to the pressure drop and the buoyancy contribute are scaled properly from reactor to experimental facility. The momentum equation for steady state single-phase natural circulation is thus satisfied for E-SCAPE.

In Table 4 are shown the scaling factors. With these relations and using the first prediction of MYRRHA natural circulation with decay heat removal (see chapter 2), we can scale the reactor dimensions and parameters to get a first ideal configuration of the test facility behavior in natural convection as it was done for forced convection.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Froude(Richardson) scaling factors</th>
</tr>
</thead>
<tbody>
<tr>
<td>(Ratio)</td>
<td>(Ratio)</td>
</tr>
<tr>
<td>Length</td>
<td>( L_R )</td>
</tr>
<tr>
<td>Diameter</td>
<td>( L_R^2 )</td>
</tr>
<tr>
<td>Area</td>
<td>( L_R^3 )</td>
</tr>
<tr>
<td>Volume</td>
<td>( \sqrt{L_R} )</td>
</tr>
<tr>
<td>Velocity</td>
<td>( \sqrt{L_R^3} )</td>
</tr>
<tr>
<td>Flow rate</td>
<td>( \sqrt{L_R} )</td>
</tr>
<tr>
<td>Pressure drops</td>
<td>( L_R )</td>
</tr>
<tr>
<td>Temperature difference</td>
<td>1</td>
</tr>
<tr>
<td>Power/vol.</td>
<td>( 1/\sqrt{L_R} )</td>
</tr>
<tr>
<td>Core Power</td>
<td>( \sqrt{L_R^3} )</td>
</tr>
<tr>
<td>Time</td>
<td>( \sqrt{L_R} )</td>
</tr>
<tr>
<td>Reynolds</td>
<td>( \sqrt{L_R^3} )</td>
</tr>
<tr>
<td>Péclet</td>
<td>( \sqrt{L_R^3} )</td>
</tr>
<tr>
<td>Richardson</td>
<td>1</td>
</tr>
<tr>
<td>Froude</td>
<td>1</td>
</tr>
<tr>
<td>Euler</td>
<td>1</td>
</tr>
<tr>
<td>Heat source</td>
<td>1</td>
</tr>
</tbody>
</table>

Table 4. Scaling parameters in natural convection for a length scaling factor \( L_R = 1/6.3 \).
Because the first goal of this study was the evaluation of natural circulation capabilities of E-SCAPE, a detailed transient consideration was not included in this scaling analysis. With these scaling parameters, a preliminary general design has been completed.

Consequently, as the first step of the analysis on loss of flow accident (LOFA) conditions, MYRRHA natural circulation capability is examined under a steady state of full loss of primary flow with decay heat power. After establishing the E-SCAPE natural circulation capability of steady state conditions from the scaling relations and comparing with the RELAP 5 model results, transient behavior with a more detailed scaling analysis should be performed. This step will not be part of this work.

In Table 5 we show the main design parameters obtained for MYRRHA and E-SCAPE utilizing the relations in Table 4 for the Froude scaling approach which was adopted.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value MYRRHA</th>
<th>Scaling factors (Froude)</th>
<th>Value E-SCAPE</th>
</tr>
</thead>
<tbody>
<tr>
<td>Vessel height [m]</td>
<td>12.6</td>
<td>1/6.3</td>
<td>2</td>
</tr>
<tr>
<td>Vessel diameter [m]</td>
<td>7.56</td>
<td>1/6.3</td>
<td>1.2</td>
</tr>
<tr>
<td>Δh between core and HX middle planes [m]</td>
<td>1</td>
<td>1/6.3</td>
<td>0.16</td>
</tr>
<tr>
<td>Vessel diameter [m]</td>
<td>7.56</td>
<td>1/6.3</td>
<td>1.2</td>
</tr>
<tr>
<td>Area Fuel Assemblies [m²]</td>
<td>0.42</td>
<td>1/39.7</td>
<td>0.01</td>
</tr>
<tr>
<td>Volume [m³]</td>
<td>420</td>
<td>1/250</td>
<td>1.68</td>
</tr>
<tr>
<td>Fuel assembly outlet velocity [m/s]</td>
<td>0.05</td>
<td>1/2.5</td>
<td>0.02</td>
</tr>
<tr>
<td>Flow rate [kg/s]</td>
<td>220</td>
<td>1/99.62</td>
<td>2.2</td>
</tr>
<tr>
<td>ΔP&lt;sub&gt;core&lt;/sub&gt; [bar] (evaluated from RELAP calculations)</td>
<td>0.08</td>
<td>1/6.3</td>
<td>0.01</td>
</tr>
<tr>
<td>Decay heat power [kW]</td>
<td>1200</td>
<td>1/99.6</td>
<td>12</td>
</tr>
<tr>
<td>Temperature difference [K]</td>
<td>37.8</td>
<td>1</td>
<td>37.8</td>
</tr>
</tbody>
</table>

Table 5. E-SCAPE design parameters from MYRRHA data in natural circulation conditions.

We can see in the Table 5 that, as expected, the scaled value of the decay heat power is easily affordable with the 100 kW heater:

\[
Q_r = \sqrt{L_r^5} \rightarrow Q_E = \sqrt{L_r^5} \cdot Q_M = 12kW
\]

This is the way to scale the heat power if we want to keep the temperature difference between the plant and the facility in natural convection and consequently the Richardson number.
A RELAP 5 simplified MYRRHA vessel scaled model will be used for validating the scaling approach in natural circulation.

In the first E-SCAPE configuration, technical reasons dictated to put the cooling loop with the heat exchangers outside the main vessel without keeping the height difference between the core and the heat exchangers mid-planes compared with a proper scaled model of MYRRHA, in which the heat exchangers are put inside the pool. E-SCAPE combined several separate effects tests in one facility but it was not designed in first instance for integral testing of the system behavior in natural convections conditions [41].

Anyway after the scaling analysis for natural circulation and its validation, it was decided to adapt the design: the heat exchanger will be put at the right distance from the core (0.16 m) but still outside the vessel. It will allow to study forced and natural convection in the same facility [42, 43].

Alternatively, a scaling strategy can be used with a different scaling factor for streamwise lengths and diameters. For such a scaling, it is possible to preserve conduction in the streamwise direction and to preserve time and Richardson numbers without resulting in practically unfeasible numbers for temperatures and power densities. However, three-dimensional phenomena such as jet behavior and mixing in large plena are distorted and also, the installation cost is high. The uniform scaling and the height-preserving scaling are both valid and need to be considered complementary [41].

In Scheme 1 a summary of the aim of this thesis work is presented.
Scheme 1. Summary of the aim of the work.
Chapter 4. Liquid-metal scaled-pool experiment E-SCAPE

4.1 Main goals of E-SCAPE

As mentioned in the previous chapters, in the frame of the THINS Project [9], the E-SCAPE (European Scaled Pool Experiment) facility under design at SCK•CEN is a thermal-hydraulic scaled model of the MYRRHA reactor and will address key phenomena relevant for liquid metal cooled reactors.

In the second chapter a basic overview of the main thermal-hydraulic that are relevant for pool type liquid metal reactors has been provided. The proper understanding of these thermal-hydraulics phenomena in the reactor pool is a critical issue in the design of MYRRHA [8, 14, 25]. Model experiments are necessary for this understanding of physics, for validating numerical tools and to qualify the design choices [14].

In accordance with these purposes SCK-CEN is designing and will execute a thermal-hydraulic experiment for assessing flow patterns in a liquid-metal pool-type fast reactor (at first instance in forced convections conditions). They will construct a 1:6 scale model of the MYRRHA reactor and measure velocity and temperature profiles [14].

We can divide the mains goals of E-SCAPE project in two parts reported in the following.

1. Reproduce as closely as possible the MYRRHA thermal-hydraulic phenomena in normal operating conditions and in different accident conditions to better understand the most important issues occurring in the MYRRHA vessel.

E-SCAPE will focus on upper and lower plenum thermal-hydraulics in forced and mixed convention conditions. As previously explained, the thermal-hydraulic challenges in these regions are [25]:

UPPER PLENUM
- Subassembly jet behavior in the core outlet region.
- Flow induced vibrations in the above core structure.
- Free surfaces oscillations resulting in thermal fatigue or gas entrainment.
- Thermal stratification and thermal fatigue.

LOWER PLENUM
- Thermal stratification and thermal fatigue.
Important are also the flow distribution e.g. between different heat exchangers, the pump jet behavior, residence times of fluid particles and the velocity and temperature fields in non-symmetrical flow conditions e.g. with pump or heat-exchanger failure [9]. Thermal stratification is an important risk in situations with a decreased flow rate as during decay heat removal.

E-SCAPE will combine several separate effects tests in one facility focusing on flow patterns in the upper and lower plenum in high flow rate conditions, but in first instance it will not be used for integral testing of the system behavior in natural transient convections conditions. The *CIRCE* experiment at ENEA Brasimone is instead designed for this purpose [44, 45, 46]. After the scaling analysis and RELAP 5 simulations the design will be adapted (in particular the position of the heat exchangers) to dealing also with natural circulation.

The definition of the scaling factors has been done based on dimensional analysis and CFD, and system code simulations. After the experiments, the measurements will be used to assess the quality of the CFD and system code predictions. One of purposes of this thesis work is to perform a RELAP 5 system analysis of the facility that will be used for the design of E-SCAPE and will be subject to code validation based on the experimental results. Consequently the second goal is as following:

2. *Compare the codes results with the experimental data.*

The current validation of the RELAP 5 model nodalization passes through the demonstration that the model reproduces the predicted steady-state conditions of the E-SCAPE facility with acceptable margins.

### 4.2 Overall configuration

According to the main purpose of E-SCAPE to be a MYRRHA scaled experimental test facility, the overall configuration is as much as possible a detailed scaled version of the reactor (see Figure 6 and Figure 7).

The following configuration represents the first concept designed for forced convection study.

The *reactor vessel* contains the prototype LBE used for the experiments: it is pumped from the lower plenum through the core by two pumps put outside the vessel in two loops which constitute the primary cooling side. The two heat exchangers are put outside of the vessel as well: this choice is principally due to practical economic considerations.
The heat power is provided by an electrical heater region that, according to the scaling analysis (see chapter 3), it is assumed to be a “black box” in which the fluid flow is unidirectional and subject to resistances uniformly distributed and uniform volumetric heating. The LBE passes through four concentric tubes surrounded by thermal resistances which give the heat power to the fluid and which offer a resistance to the flowing fluid. To obtain the required drop of pressure through the core two perforated plates are put in the inlet and outlet regions.

Downstream the core, the LBE enters in the core barrel, a big tube getting up until the gas plenum. In the barrel wall there are holes so the hot liquid metal gets gradually to the upper plenum region. On the upper plenum two blocks of three tubes each are housed: the pumped LBE flows through the holes of the two tubes going up out of the vessel and reaches the piping side outside, where the heat exchangers are put.

After the cooling, the LBE is finally pushed by the pumps into the middle tube of each pipe block, coming back in the lower plenum passing through a nozzle which allows to simulate a jet behavior.

As MYRRHA, E-SCAPE is also characterized by two free surfaces levels: the first one is the upper plenum level and the second one is connected to the lower plenum. The two free surfaces are in contact with the common gas plenum. The level difference between the two free surfaces is due to pressure drop occurring in the core region and gives an indication on size. It will be an important issue to take into account.

The pressure in the gas plenum is kept in normal operating conditions at the value of 2 bar to avoid low pressures in the piping above the vessel. The Figure 6 shows the overall configuration of the E-SCAPE project with the cooling piping out of the vessel and the heat exchangers. Figure 7 shows the detailed E-SCAPE reactor vessel configuration.
Figure 6. *E-SCAPE* layout (one cooling side).
Figure 7. *E-SCAPE reactor Vessel.*
4.3 Characteristics of the main components

The description of the components in this paragraph, together with the main dimensions indicated in Figure 7, determine the relevant parameters for system analysis.

In E-SCAPE different experiments will be performed, consequently different design options will be taken into account: to perform the RELAP 5 simulations we used the parameters resulting by the Froude scaling analysis (see chapter 3).

The heater as said above is characterized by four concentric channels surrounded by electric resistances which provide the thermal power as shown in Figure 8.

The nominal thermal power is fixed according to the scaling analysis (see later) and technical limitations to the value of 100 kW.

Figure 8. A View of the heater and of the inlet and outlet plates.
The inlet plate is composed by 156 holes, while the outlet one is composed by 150 holes. The pitch is 11 mm and the diameter of each sub channel is 11 mm as well. The plate height is 30 mm. These components are very important in the design of the experimental facility because changing the number and the diameter of the holes we can obtain the desired values of pressure drop and velocities at the heater outlet.

The two **heat exchangers** are composed of two concentric counter-flow tubes where the LBE passes through the internal tube and the secondary cooling fluid passes through the external tube. The dimensions are the following:

\[
D_1 = 0.0828\,\text{m} \\
D_2 = 0.0889\,\text{m} \\
D_3 = 0.1082\,\text{m} \\
h = 0.5\,\text{m}
\]

The secondary cooling fluid is supposed to be oil.

In Table 6 below the main characteristics of the cooling fluid are shown.

<table>
<thead>
<tr>
<th></th>
<th>LBE</th>
<th>OIL</th>
</tr>
</thead>
<tbody>
<tr>
<td>( T_{LBE_{\text{out}}} )</td>
<td>283 °C</td>
<td>( T_{OIL_{\text{in/out}}} )</td>
</tr>
<tr>
<td>( C_p )</td>
<td>145,0992 J/kg·°C</td>
<td>( C_p )</td>
</tr>
<tr>
<td>( \rho )</td>
<td>10344.87 kg/m(^3)</td>
<td>( \rho )</td>
</tr>
<tr>
<td>( \mu )</td>
<td>0.001841 kg/ms</td>
<td>( \mu )</td>
</tr>
<tr>
<td>( k )</td>
<td>11,82046 W/mK</td>
<td>( k )</td>
</tr>
</tbody>
</table>

Table 6: Cooling fluid characteristics.

The two **pumps** are centrifugal and vertically orientated.

**Preliminary data:**
- Pump nominal flow rate: 25 kg/s
- Head: 0.38 m
- Operating speed: 110 rad/s
- Power: 160 W (efficiency 80%)
• Torque: 1.5 Nm
• Rotating part inertia: 0.02 kg \cdot m^2
• Pump impeller diameter: 90 mm
• Length: 0.2 m

The **barrel** component preliminary characteristics are the following:

- \( D_{\text{in}} = 0.224 \text{m} \)
- \( D_{\text{out}} = 0.228 \text{ m} \)
- \( L\text{ength} = 1.355 \text{m} \)
- \( N_{\text{holes}} = 120 \)
- \( D_{\text{hole}} = 0.03184 \text{m} \)

The design of this component is very important because according to the number and the geometry of the holes we can obtain two different liquid metal levels inside the pool: the upper plenum level, and the liquid metal contained in the barrel according to the pressure drop across it. The barrel layout is also important for the collocation of the experimental tools.
Chapter 5. RELAP 5 simulations

5.1 Transients analyzed

A detailed analysis of the MYRRHA behavior during operational and accidental transients using a system code is mandatory. It allows to assess in detail whether the current plant layout can accommodate these transients or whether design measures need to be taken to improve the reactor response.

Particularities of system analysis of the case at hand are, among the others, the use of a liquid metal coolant as fluid, the flow pattern in the pool type reactor vessel, the possible oscillations of the free surfaces, etc. (see chapter 4). The system analysis code should be able to deal with these particularities directly or possibly represent their effect on the reactor dynamics.

The E-SCAPE facility is a thermal hydraulic scaled model of MYRRHA. The installation, as explained in chapter 4 should allow:

- to investigate the MYRRHA behavior in normal operating conditions;
- to validate system codes that predict MYRRHA behavior.

On the other hand, within the design process of E-SCAPE, the system codes are used to investigate whether the experimental facility correctly reproduces the expected MYRRHA behavior.

In this chapter we analyze in detail the E-SCAPE model for forced convection developed following the Froude number scaling approach. With this model we can study also loss of heat sink transients. On the other hand, to validate the scaling approach for natural convection flow, a scaled simplified configuration of MYRRHA having the heat exchangers inside the pool, at the right scaled height, has been developed. We will call this model “MYRRHA scaled”.

Different operational and accidental transients are to be considered for the analysis:

- **Start-up transient (E-SCAPE)**

The goal of this simulation is to analyze the behavior of the facility during the start-up transient until operational conditions are obtained. The analysis will focus on the time trend of some of the parameters in the zones of interest, such as temperature, pressure and flow rate. The analysis will provide a first thermal hydraulic characterization of the
facility during normal operating conditions. The results will be used to validate Froude scaling approach in forced convection flow.

- **Asymmetric loss of heat sink (E-SCAPE)**
  The loss of cooling flow through the secondary side of one heat exchanger will have an immediate impact on temperatures and therefore densities. They will in their turn influence the free surfaces heights. Loss of cooling will be considered with heat power shut down.

- **Symmetric loss of heat sink (E-SCAPE)**
  The loss of cooling flow through the secondary side of both the heat exchangers will have an immediate impact on temperatures and therefore densities as well. These will in their turn influence the free surfaces heights. Symmetric loss of cooling will be considered with and without heat power shut down.

- **Symmetric main pump failure (MYRRHA SCALED)**
  Blockage or run-down of both the mains pumps will immediately affect the surface heights in the E-SCAPE vessel as well. Moreover, the cooling capabilities will be completely lost except for a low contribution due to natural convection. Also symmetric pump failure will be considered with heat power shut-down.

The test matrix of the simulations performed in the present work dealing with normal operating conditions is shown in Table 7, which summarizes the adopted boundary conditions and the main variables that were monitored, while the Table 8 and Table 9 summarize the transient simulation conditions and the main parameters to be taken into account for E-SCAPE and MYRRHA scaled, respectively.
**Test A**

E-SCAPE thermal-hydraulic analysis in normal operating condition

<table>
<thead>
<tr>
<th>Kind of simulation</th>
<th>E-SCAPE start-up transient</th>
</tr>
</thead>
</table>
| **Operating conditions** | • Heat power: 100 kW  
                          • Temperature: 283.5 °C  
                          • ΔT core: 13.8 °C  
                          • Core flow rate: 50 kg/s  
                          • ΔP_{friction, core}: 0.33 bar |
| **Variables to be checked** | • T_{in} and T_{out} heat exchangers primary side  
                          • T_{out} heat exchangers secondary side  
                          • Liquid level in the upper plenum  
                          • Δh free surfaces  
                          • T_{in} and T_{out} heater  
                          • Δp pumps  
                          • Δp core  
                          • Flow rate in the heater  
                          • Flow rate in the heat exchangers  
                          • Upper plenum temperatures  
                          • Lower plenum temperatures |
| **Main goals** | • Thermal-hydraulic characterization of the test facility  
                          • Verify the coupling primary-secondary side of the heat exchangers  
                          • Check up of the LBE temperatures in the key positions of the plant  
                          • Free surface stability and Δh correspondence with the drop of pressure in the core  
                          • Verify the flow patterns and the presence of thermal stratification  
                          • Validation of the Froude scaling approach |

Table 7. *Test matrix in normal operating conditions.*
## Test B

### E-SCAPE Incidental transient analysis

<table>
<thead>
<tr>
<th>Kind of simulation</th>
<th>Test B1</th>
<th>Test B2</th>
<th>Test B3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Symmetric loss of sink (With decay heat removal)</td>
<td>Asymmetric loss of sink (With decay heat removal)</td>
<td>Asymmetric loss of sink (Keeping 100kW power)</td>
<td></td>
</tr>
</tbody>
</table>

### Initial conditions

- Heat power: 100 kW
- Temperature: 283.5 °C
- $\Delta T$ core: 13.8 °C
- Core flow rate: 50 kg/s
- $\Delta P_{\text{friction core}}$: 0.33 bar

### Variables to be checked

- $T_{in}$ and $T_{out}$ in the heat exchangers
- Pumps flow rate evolution
- Core flow rate
- Upper Plenum temperatures
- Lower Plenum temperatures
- $\Delta h$ free surfaces

### Main goals

- System analysis in incidental transient for E-SCAPE and consequently for MYRRHA scaling up the results
- System analysis in incidental transient for E-SCAPE and consequently for MYRRHA scaling up the results
- Valuate the maximum temperature increase trend with loss of sink incidental transient for the facility

<table>
<thead>
<tr>
<th>Variables to be checked</th>
<th>Test B1</th>
<th>Test B2</th>
<th>Test B3</th>
</tr>
</thead>
<tbody>
<tr>
<td>$T_{in}$ and $T_{out}$ in the heat exchangers</td>
<td>$T_{in}$ and $T_{out}$ in the heat exchangers</td>
<td>$T_{in}$ and $T_{out}$ in the heater</td>
<td>$T_{in}$ and $T_{out}$ in the heater</td>
</tr>
<tr>
<td>Pumps flow rate evolution</td>
<td>Pumps flow rate evolution</td>
<td>$\Delta h$ free surfaces</td>
<td>$\Delta h$ free surfaces</td>
</tr>
<tr>
<td>Core flow rate</td>
<td>Core flow rate</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Upper Plenum temperatures</td>
<td>Upper Plenum temperatures</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Lower Plenum temperatures</td>
<td>Lower Plenum temperatures</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$\Delta h$ free surfaces</td>
<td>$\Delta h$ free surfaces</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

### Table 8.

Table 8. Test matrix for transient conditions in E-SCAPE.


<table>
<thead>
<tr>
<th>Test C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal-hydraulic analysis in natural circulation conditions</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Kind of simulation</th>
<th>MYRRHA-SCALED</th>
</tr>
</thead>
<tbody>
<tr>
<td>Scaling driving number</td>
<td>Richardson (Froude)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Initial conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>• Heat power: 100 kW</td>
</tr>
<tr>
<td>• Temperature: 300 °C</td>
</tr>
<tr>
<td>• ΔT core: 13.8 °C</td>
</tr>
<tr>
<td>• Core flow rate: 50 kg/s</td>
</tr>
<tr>
<td>• ΔP_{friction} core: 0.33 bar</td>
</tr>
<tr>
<td>• Δh core-HX: 0.16 m</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Variables to be checked</th>
</tr>
</thead>
<tbody>
<tr>
<td>• $T_{in}$ and $T_{out}$ heat exchangers primary side</td>
</tr>
<tr>
<td>• $T_{in}$ and $T_{out}$ core</td>
</tr>
<tr>
<td>• Flow rate in the core</td>
</tr>
<tr>
<td>• Flow rate in the heat exchangers</td>
</tr>
<tr>
<td>• Upper plenum temperatures</td>
</tr>
<tr>
<td>• Lower plenum temperatures</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Main goals</th>
</tr>
</thead>
<tbody>
<tr>
<td>• MYRRHA system analysis in incidental transient</td>
</tr>
<tr>
<td>• Validation of the scaling approach for natural circulation</td>
</tr>
</tbody>
</table>

Table 9. Test matrix for natural circulation conditions in MYRRHA scaled.

5.2 E-SCAPE facility RELAP 5 Nodalization

The E-SCAPE RELAP5 nodalization has been developed mainly on the basis of the reference data shown in the previous sections. The thermal-hydraulic analysis has been performed using the system code RELAP5/mod.3.3, modified at the University of Pisa to allow for LBE as
cooling fluid [47, 48, 49]. This version of the code was already applied in post-test analyses of experiments performed with CHEOPE, NACIE and CIRCE facilities installed at the ENEA Brasimone Research Centre [48, 49].

The Light Water Reactor (LWR) transient analysis code, RELAP5, was developed at the Idaho National Engineering Laboratory (INEL) for the U.S. Nuclear Regulatory Commission (NRC). It has been developed for best-estimate transient simulation of light water reactor coolant systems during postulated accidents [48]. RELAP 5 has also been used as the basis for a nuclear plant analyzer. Specific applications have included simulations of transients in LWR systems such as loss of coolant (LOCA), anticipated transients without scram (ATWS), and operational transients such as loss of feed-water, loss of offsite power, station blackout, etc.

A generic modeling approach is used that allows simulating a variety of thermal hydraulic systems: it can be used for simulation of a wide variety of hydraulic and thermal transients in both nuclear and nonnuclear systems involving mixtures of steam, water, non-condensables [50]. The code includes many generic component models from which general systems can be simulated like pumps, valves, pipes, heat releasing or absorbing structures, reactor point kinetics, electric heaters, jet pumps, turbines, separators, accumulators, and control system components. In addition, special process models are included for effects such as form loss, flow at an abrupt area change, branching, choked flow, boron tracking, and non-condensible gas transport [50]. The system mathematical models are coupled into an efficient code structure based on a non-homogeneous and non-equilibrium model for the two-phase system that is solved by a fast, partially implicit numerical scheme to permit economical calculation of system transients or with a nearly-implicit numerical scheme [50]. In this work we used the partially-implicit (or semi-implicit) numerical scheme.

During the years the code has been adapted for using cooling fluids different from the steam-water mixture, implementing the thermodynamic properties of Pb and of the alloy Pb-Bi [47, 48, 49].

Concerning heat transfer, the one-dimensional heat conduction problem is solved using heat structures with convective boundary conditions.

The heat transfer correlation used in this work is the Seban & Shimazaki correlation which deals with liquid metal flow in stationary conditions (constant clad temperature) [51]:

$$Nu = 5.0 + 0.025 Pe^{0.8}$$
In Figure 9 the RELAP 5 nodalization is shown, while in Table 10 the main components used in the model are described.

<table>
<thead>
<tr>
<th>COMPONENT</th>
<th>DESCRIPTION</th>
</tr>
</thead>
<tbody>
<tr>
<td>TMDPVOL</td>
<td>\textit{TIME DEPENDENT VOLUME}: Volume in which the thermodynamic conditions are allowed to change as function of time.</td>
</tr>
<tr>
<td>BRANCH</td>
<td>It is a \textit{single volume} which has the possibility to be connected with more than one component. In particular a branch could have more than two junctions.</td>
</tr>
<tr>
<td>J</td>
<td>\textit{SINGLE JUNCTION}: junction between elements of the same type or of different type.</td>
</tr>
<tr>
<td>TMDPJUN</td>
<td>\textit{TIME DEPENDENT JUNCTION}: junction in which it is possible to impose a flow rate variable over time.</td>
</tr>
<tr>
<td>PUMP</td>
<td>Hydrodynamic component \textit{pump}, characterized by its own characteristic curve pressure head-flow rate.</td>
</tr>
<tr>
<td>PIPE</td>
<td>Tube composed of single volumes connected by junctions.</td>
</tr>
<tr>
<td>CROSS-FLOW JUCNTIONS</td>
<td>Single junction which allows lateral connections.</td>
</tr>
</tbody>
</table>

*Table 10. Hydrodynamic components used for the RELAP nodalization of E-SCAPE.*
Figure 9: RELAP 5 nodalization of the E-SCAPE facility.
The lower plenum has been simulated with the components pipes 10, 20, 30. These components are connected to each other by cross-flow junctions trying to obtain a sort of 3D description, considering the difficulties to simulate a pool-type reactor with a 1D system code. There is no difference in the application of the conservation equations to the normal and cross-flow types of connections. The only difference is that the normal term is applied to the flow that would occur in a strictly one-dimensional volume; cross-flow is an approximation to multidimensional effects consisting of applying the one-dimensional momentum equation to each of the coordinate directions in use. To give some perspective to the approximation, the three-dimensional momentum equation contains nine terms for momentum flux; the momentum in each of the three directions being convected by velocities in the three directions. In the cross-flow model, only three momentum flux terms are used the momentum in each direction convected by velocity in the same direction [50].

The volumes 10 and 30 are smaller than the volume 20: in this way we tried to simulate the pump jet behavior in that zone; being impossible to get detailed information in the lowest part of lower plenum, if it was simulated just with one big volume.

The core is simulated by the component pipe 50 and the heat power is provided by the heat structure which imposes to the volume a time dependent heat power. The pressure drop occurring in that region has been taken into account inserting an appropriate localized drop using form loss coefficients. These coefficients have been considered as a design variables necessary to get the required nominal conditions.

The core barrel has been simulated with the component pipe 70 connected by cross flow junctions to the upper plenum, simulated with the components pipe 100 and pipe 400 which have been connected to each other with cross flow junctions as well.

The LBE volumes between the two diaphragm plates are isolated thermally from the lower plenum: they are obtained imposing appropriate horizontal heat structures. The low flow rate recirculation foreseen in that zone has been allowed imposing an high pressure loss coefficient between the upper plenum and the volumes pipe 102 and pipe 402: in this way almost all the LBE flow rate is directed to the two blocks representing the path to the heat exchangers. The components of these two blocks are simulated by the pipes 110, 120, 130, 140 on the left side, and by pipes 420, 430, 440, 450 on the right side. The volumes 102 and 402 are also connected each other by cross-flow junctions.

The first free surface inside the pool is related to the LBE level in the upper plenum and has been obtained imposing the presence of an argon fraction in pipe 90, and a volume
of argon in the *time dependent volume 92*, maintained at 2 bara in normal operating conditions.

The second free surface connected to the lower plenum, whose height difference with respect to the first free surface indicates the LBE level change due to the core pressure drop, is obtained in the same way, imposing an argon fraction in *pipe 310 and pipe 640*, and connecting both together and also with the upper part of the *pipe 90*, which is filled by argon in the last top volumes. In this way, the two free surfaces experience the same boundary condition, corresponding to the real plant configuration in which they are both connected with a single gas plenum.

The two main pumps have been simulated by the hydrodynamic components *pump 222* and *pump 532* that follow the characteristic curve according with the main parameters described in previous chapters.

The heat exchangers have been simulated with two heat structures that exchange at one side with LBE (*pipe 240 and pipe 550*) and at the other side with *pipes 370 and 770* in which water flows in counter-flow.

The secondary cooling fluid is water because the code does not allow to deal with oil. The pumps of the secondary side have been simulated with the components *time depending junction 360* and *time depending junction 760* which impose a variable flow rate as a function of time. This component is equivalent to a volumetric pump where the flow rate is independent from the hydraulic characteristics of the loop. The tertiary cooling loop has not been taken into account in this analysis.

In all components, thermal structures were also taken into account with the purpose to consider their effect on the analyzed transients due to their thermal inertia. Apart from the HX, all the external surfaces of the thermal structures of the piping line are considered exchanging convective heat with external air, fixing the heat transfer coefficient at the value of 1 W/(m² K). Also the components inside the vessel exchange heat with each other: in particular the heat transfer between the cold LBE coming down from the heat exchangers (*pipe 270, pipe 580*) and the hot LBE in the upper plenum has been considered as a particularly important aspect to be taken into account.

Other important considerations for the analysis envisaged here are:

- the wall roughness of the hydrodynamic components has been put equal to $4.5 \cdot 10^{-3}$ m;
- Regarding the main pumps, the values of head and speed have been adapted as design parameters to get the nominal flow rate conditions, since the actual characteristic curve is not yet available;
• The LBE conditioning unit, fuel storage and flow rate by-pass have not been considered.
• The structural material is considered to be AISI316 stainless steel.

Geometrical description of the components

Below, the adopted nodalization is explained in detail starting with the primary loop and then with the secondary loop of the Heat Exchangers (see Figure 9).

• Primary side

*PIPE 10*
The main characteristics of this RELAP5 hydraulic component, placed in the bottom region of the left side of the primary loop (see Figure 9), are:
\[ V = 0.15 \text{ m}^3 \]
\[ L = 0.449 \text{ m (vertical, upward)} \]

*CROSS-FLOW JUNCTIONS 11, 12, 13, 14, 15*
These connect the outlet lateral section of the *pipe 10* with the left lateral section of the *pipe 20*, with a flow area that is taken equal to the minimum flow area of the adjoining volumes. These components have been chosen to try to better reproduce the 3D mixing phenomena occurring in the lower plenum. The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

*PIPE 20*
The pipe represents the lower plenum main volume, placed in the bottom region of the centre side of the primary loop (see Figure). The main characteristics are:
\[ V = 0.225 \text{ m}^3 \]
\[ L = 0.449 \text{ m (vertical, upward)} \]

*PIPE 30*
This pipe represents the lower plenum right volume, placed in the bottom region of the vessel (see Figure), the main characteristics are:
\[ V = 0.15 \text{ m}^3 \]
\[ L = 0.449 \text{ m (vertical, upward)} \]
**CROSS-FLOW JUNCTIONS 21, 22, 23, 24, 25**

These connect the left lateral section of the pipe 30 with the right lateral section of the pipe 20, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**CROSS-FLOW JUNCTIONS 800, 801, 802, 803, 804**

These components are not shown in the figure and they connect the left lateral section of the pipe 10 with the right lateral section of the pipe 30, with the aim of giving a sort of 3D correspondence between the three volumes which constitute the lower plenum. The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**SINGLE JUNCTION 36**

It connects the outlet section of the pipe 20 with the inlet section of the pipe 40, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure). The pressure loss coefficient considered in this junction is 10.2 both for forward and reversed flow.

**PIPE 40**

This pipe represents the core inlet plate of. The main geometrical parameters of this component are:

\[ D_i = D_{hyd} = 0.011 \text{ m} \]

\[ A_{c-s} = 0.0148 \text{ m}^2 \] (this value is different from the MYRRHA scaled fuel assembly flow area used for the scaling, because the first E-SCAPE design referred to an older version of the plant).

\[ L = 0.03 \text{ m} \] (vertical, upward)

An internal pressure loss coefficient of 3.8 both for forward and reversed flow has been imposed.

**SINGLE JUNCTION 45**

It connects the outlet section of the pipe 40 with the inlet section of the pipe 50 (core), with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure).
The pressure loss coefficient considered in this junction is 10.2 both for forward and reversed flow.

**PIPE 50**
The main geometrical parameters of this component, which represents the core simulator, are:
\[ D_i = D_{hyd} = 0.0428 \text{ m} \]
\[ A_{c-s} = 0.028 \text{ m}^2 \]
\[ L = 0.289 \text{ m (vertical, downward)} \]
\[ \varepsilon = 3.545 \cdot 10^{-3} \text{ m (wall roughness)} \]

The generated heat power has been imposed directly within the wall of this pipe.
To represent the internal drop of pressure in the core an internal pressure loss coefficient of 4 both for forward and reversed flow has also been imposed.

**SINGLE JUNCTION 55**
It connects the outlet section of the core with the inlet section of the pipe 60 (outlet core plate), with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 10.2 both for forward and reversed flow.

**PIPE 60**
This pipe represents the outlet core plate. The main geometrical parameters of this component are:
\[ D_i = D_{hyd} = 0.011 \text{ m} \]
\[ A_{c-s} = 0.014255 \text{ m}^2 \]
\[ L = 0.03 \text{ m (vertical, upward)} \]
Internal pressure loss coefficient of 3.8 both for forward and reversed flow has been imposed.

**SINGLE JUNCTION 65**
It connects the outlet core plate with the inlet section of the pipe 70 (core barrel), with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 11 both for forward and reverse flow.
\textit{PIPE 70}  
This \textit{pipe} represents the core barrel. The main geometrical parameters of this component are:  
\begin{align*}
  D_i &= D_{hyd} = 0.193 \text{ m} \\
  A_{c-s} &= 0.038 \text{ m}^2 \\
  L &= 0.555 \text{ m (vertical, upward)}
\end{align*}  

\textit{CROSS-FLOW JUNCTIONS 71, 72, 73, 74, 75}  
They connects the left lateral section of the \textit{pipe 70} with the right lateral section of the \textit{pipe 100}, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow at this stage, because the number and the dimensions of the holes are still under study.  

\textit{CROSS-FLOW JUNCTIONS 76, 77, 78, 79, 80}  
They connects the right lateral section of the \textit{pipe 70} with the left lateral section of the \textit{pipe 400}, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9).  

\textit{CROSS-FLOW JUNCTIONS 46, 47, 48, 49, 51}  
These components are not shown in the figure and they connect the left lateral section of the \textit{pipe 100} with the right lateral section of the \textit{pipe 400}, with the aim of giving a sort of 3D correspondence between the two volumes which constitute the upper plenum.  

\textit{SINGLE JUNCTION 85}  
It connects the outlet section of the barrel with the inlet section of the pipe 90, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.  

\textit{PIPE 90}  
It is only partially filled with LBE. In particular, with the primary fluid at rest, around 38.5\% of its volume is filled with LBE and the remaining 61.5\% with argon. The upper plenum free surface is thus located inside this \textit{pipe}. The main geometrical parameters of this component, located on the top of the reactor vessel (see Figure 9), are:
$V = 0.4431 \text{ m}^3$
$L = 0.7 \text{ m (vertical, upward)}$

**SINGLE JUNCTION 91**
It connects outlet section of the pipe 90 with the inlet section of the time dependent volume 92 (gas plenum), with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**TIME DEPENDENT VOLUME 92**
It is totally filled with argon which has a pressure of 3 bara and temperature of 573.15 K with the primary fluid at rest, and 2 bara with a temperature of 573.15 K during all the simulated transients. This fact is to avoid numerical problems at start-up.

**SINGLE JUNCTIONS 95,395**
They connect inlet section of the pipe 90 with the inlet section of the pipe 100 and 400 respectively (upper plenum), with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**PIPES 100,400**
These pipes represent the upper plenum. The main geometrical parameters of this component are:
$V = 0.175 \text{ m}^3$
$L = 0.555 \text{ m (vertical, downward)}$

**SINGLE JUNCTIONS 101, 401**
They connect outlet the sections of the pipes 100 and 400 with the inlet section of the pipes 102 and 402 respectively, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9 ). The pressure loss coefficient considered in this junction is 100 both for forward and reversed flow: this is due to avoid high liquid flow rate in components 102 and 402 (except a small flow rate due to recirculation) which, according to the design, should are stagnant components.
**PIPS 102,402**

These *pipes* represent the LBE volume between the two diaphragm plates. The main geometrical parameters of these components are:

\[ V = 0.137 \text{ m}^3 \]

\[ L = 0.349 \text{ m (vertical, downward)} \]

**BRANCH 105, 410**

They are volumes with three “internal” junctions. The first junction (J-1) connects the outlet section of the *pipe* 100 (400) with the inlet section of the *branch* 105 (410) with a flow area that is taken equal to the minimum flow area of the adjoining volumes; the pressure loss coefficient considered in this *junction* is 0.5 both for forward and reversed flow.

The second and the third *junctions* (J-2, J-3) connect the outlet section of the *branch* 105 (410) with the inlet section of the *pipes* 110 (420) and 120 (430) respectively, with a flow area that is taken equal to the minimum flow area of the adjoining volumes; the pressure loss coefficients considered in these *junctions* is 0.2 both for forward and reverse flow. The main geometrical parameters of this component (see Figure 9) are:

\[ A_{c,s} = 0.040 \text{ m}^2 \]

\[ L = 0.116 \text{ m (vertical, upward)} \]

**PIPS 110-120, 420-430**

These *pipes* represent the heat exchanger outer geometry and are the first part of the tubes rising up to the piping outside the vessel.

The main geometrical parameters of these components are:

\[ A_{c,s} = 0.0142 \text{ m}^2 \]

\[ L = 0.439 \text{ m (vertical, upward)} \]

**SINGLE JUNCTIONS 115-125, 425-435**

They connect outlet sections of the *pipes* 110(420) and 120 (430), with the inlet section of the *pipes* 130 (440), 140 (450) respectively, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**PIPS 130-140, 440-450**

These *pipes* represent the second part of the tubes rising up to the piping outside the vessel.
The main geometrical parameters of these components are:
\[ A_{c-s} = 0.0142 \text{ m}^2 \]
\[ L = 0.7 \text{ m (vertical, upward)} \]

**PIPES 150-160, 460-470**

These pipes represent the first part of the piping loop outside the vessel. The main geometrical parameters of these components are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 0.5 \text{ m (vertical, upward)} \]

**PIPES 170-180, 480-490**

These pipes represent the piping loop outside the vessel. The main geometrical parameters of these components are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 1 \text{ m (horizontal)} \]

**PIPES 190-200, 500-510**

These pipes represent the piping loop outside the vessel. The main geometrical parameters of these components are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 0.4 \text{ m (vertical, downward)} \]

**BRANCH 210, 520**

They are volumes with two “internal” junctions. The first junction (J-1) connects the outlet section of the pipe 190 (500) with the inlet section of the branch 210 (520) with a flow area that is taken equal to the minimum flow area of the adjoining volumes; the pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

The second junction (J-2) connects the outlet section of the pipe 200 (510) with the inlet section of the branch 210 (520) with a flow area that is taken equal to the minimum flow area of the adjoining volumes; the pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow. The main geometrical parameters of this component (see Figure) are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 0.1 \text{ m (vertical, downward)} \]
PIPES 220, 530
These pipes represent the piping loop outside the vessel. The main geometrical parameters of these components are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 0.5 \text{ m (horizontal)} \]

PUMPS 222, 532
They connect the outlet section of the pipe 220 (530) with the inlet section of the pipe 230(540), which in Figure 9, belong to the piping line outside the vessel, and with a flow area that has been taken equal to the minimum flow area of the adjoining volumes. The pump characteristics, as head, torque, nominal pump velocity, etc., were chosen so that the pressure-head-flow rate curve approaches as close as possible the one provided in the specifications.
\[ A_{c-s} = 0.0063 \text{ m}^2 \]
\[ L = 0.20 \text{ m (vertical)} \]

PIPES 230, 540
These pipes represent the piping loop outside the vessel. The main geometrical parameters of these components are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 0.4 \text{ m (horizontal)} \]

PIPES 240, 550
These pipes represent the primary side of the heat exchanger. In particular, the LBE flows through the inner pipes. The main geometrical parameters of this component, are:
\[ A_{c-s} = 0.00538 \text{ m}^2 \]
\[ L = 0.5 \text{ m (vertical, upward)} \]

PIPES 250, 560
These pipes represent the piping loop outside the vessel. The main geometrical parameters of these components are:
\[ A_{c-s} = 0.005 \text{ m}^2 \]
\[ L = 2 \text{ m (horizontal)} \]
PIPES 260, 570
These pipes represent the piping loop outside the vessel. The main geometrical parameters of these components are:

\[ A_{c-s} = 0.005 \, \text{m}^2 \]
\[ L = 0.5 \, \text{m (vertical, downward)} \]

PIPES 270, 580
These pipes represent the return line from the heat exchangers to the reactor vessel. The main geometrical parameters of these components are:

\[ A_{c-s} = 0.0054 \, \text{m}^2 \]
\[ L = 1.254 \, \text{m (vertical, downward)} \]

SINGLE JUNCTIONS 275, 585
They connect the outlet sections of pipe 270 (580), with the inlet section of pipe 280 (600), with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

PIPES 280, 600
The main geometrical parameters of these components are:

\[ A_{c-s} = 0.0054 \, \text{m}^2 \]
\[ L = 0.349 \, \text{m (vertical, downward)} \]
\[ D = 0.12 \, \text{m} \]

SINGLE JUNCTIONS 285, 605
They connect outlet sections of pipe 280 (600), with the inlet section of pipe 10 (30) (lower plenum, with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

SINGLE JUNCTIONS 286, 615
They connect outlet sections of pipe 10 (30) (lower plenum), with the inlet section of pipe 290 (620), with a flow area that is taken equal to the minimum flow area of the adjoining volumes
(see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**PIPES 290, 610**
The main geometrical parameters of these components are:
\[ Ac-s = 0.04615 \text{ m}^2 \]
\[ L = 0.349 \text{ m (vertical, downward)} \]

**SINGLE JUNCTIONS 295,625**
They connect the outlet section of pipe 290 (620) with the inlet section pipe 300 (630) with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**PIPES 300, 630**
The main geometrical parameters of these components are:
\[ Ac-s = 0.04615 \text{ m}^2 \]
\[ L = 0.555 \text{ m (vertical, downward)} \]

**SINGLE JUNCTIONS 305,635**
They connect the outlet sections of pipe 300 (630) with the inlet section of pipe 310 (640) with a flow area that is taken equal to the minimum flow area of the adjoining volumes (see Figure 9). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

**PIPES 310, 640**
They are only partially filled with LBE. In particular, with the primary fluid at rest 38.5% of its volume is filled with LBE and the remaining 61.5% with argon. The free surface connected to the lower plenum is just that inside these pipes. The main geometrical parameters of these components are:
\[ Ac-s = 0.04615 \text{ m}^2 \]
\[ L = 0.7 \text{ m (vertical, downward)} \]
CROSS-FLOW JUNCTIONS 315, 635
They connect the last part of the pipes 310 and 640 (filled by gas) with the gas plenum (pipe 90). The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

CROSS-FLOW JUNCTIONS 316, 317, 318, 319, 320, 321, 322
They connect the left lateral section of the pipe 310 with the right lateral section of the pipe 640, with the aim of giving a sort of 3D correspondence between the two volumes which constitute the annulus of the second free surface. The pressure loss coefficient considered in this junction is 0.0 both for forward and reversed flow.

• Secondary side
TIME DEPENDENT VOLUMES 350, 750
They are totally filled with water at pressure of 1 bar and a temperature of 323.15 K during all the simulated transients. The main geometrical parameters of these components, (see Figure 9), are:
\[ V = 10^6 \text{ m}^3 \]
\[ L = 10 \text{ m (vertical, upward)} \]

TIME DEPENDENT JUNCTIONS 360, 760
It connects the inlet section of the components 320-760 with the inlet section of the pipes 370-770 respectively, with a flow area that is taken equal to the minimum flow area of the adjoining volumes. By this type of junction we have imposed the mass flow rate of the water in the secondary side of the HX.

PIPES 370, 770
These pipes represent the secondary side of the heat exchanger. In particular, the water flows through the outer side of the two concentric tubes in counter flow. The main geometrical parameters of these components are (see Figure 9), are:
\[ A_{c-s} = 0.00298 \text{ m}^2 \]
\[ L = 0.5 \text{ m (vertical, downward)} \]
\[ D_{hyd} = 0.0193 \]
SINGLE JUNCTION 305
It connects the outlet section of the pipes 370-770 with the inlet section of the time dependent volumes 390 and 790 respectively, with a flow area that is taken equal to the minimum flow area of the adjoining volumes. The pressure loss coefficient set in this junction is 0.0 both for forward and reversed flow.

TIME DEPENDENT VOLUMES 390, 790
They are totally filled with water at pressure of 1 bar and a temperature of 363.15 K during all the simulated transients.
The main geometrical parameters of these components, are:
\[ V = 10^6 \, m^3 \]
\[ L = 10 \, m \text{ (vertical, upward)} \]

5.3 Scaled MYRRHA REALP 5 nodalization

In Figure 10 the scaled model of MYRRHA is shown in detail. The main differences between this configuration and the E-SCAPE model are:

- The heat exchangers are put inside the reactor vessel.
- The two main pumps are consequently put in the reactor vessel as well.
- The number of the heat exchangers, according with the real MYRRHA configuration, is 4 and the height of each heat exchanger is 0.25 m instead of the 0.5 m before.
- The total heat exchanger heat transfer surface between the two configurations is maintained.
- The components pipe 100 and pipe 400 are larger than in E-SCAPE configuration because they have to include the LBE volume of the two three-pipes blocks (two rising up to the piping and one coming down after cooling) to maintain the same total LBE volume in the vessel.
- In this case the pressure in the gas plenum could be maintained at 1 bar.

An important constraint that has been taken into account is to maintain the height difference between the core thermal centre and the heat exchangers thermal centre, which in MYRRHA is 1 m and scale it with a factor equal to 6.3. This fact is important to properly simulate natural convective conditions. If we scale-up this configuration it will be possible to obtain a
full scale E-SCAPE model which can be used for a first estimation of MYRRHA natural convection capabilities and verify the analytic solution presented in chapter 2.

Figure 10. *RELAP 5 nodalization of a 1/6 scaled version of MYRRHA.*
Chapter 6. Analysis of the results

6.1 TEST A

Boundary conditions Test A

In this simulation, the pump is kept switched off for the first 10 s, in order to obtain correct pressure distribution in a plant with stagnant fluid conditions. Over the next 100 s the pump speed is changed linearly from 0 to 110 rad/s and then maintained constant until the end of the simulation (see Figure 11).

![Figure 11. Main pump rotation velocity at the start-up.](image)

The thermal power generated in the heater is released in pipe 50 and its time trend is shown in Figure 12. In the first 500 s no thermal power is delivered, in order to avoid a too high temperature increase until the mass flow rate large enough. Between 500 and 1000 s the thermal power is linearly increased up to the value of 100 kW and it is maintained at this value.

After 500 s from the start of the transient, the water flow rate in the secondary side of the heat exchanger (HX) is increased linearly from 0 to 2.5 kg/s and is then maintained constant (see Figure 13); the inlet temperature in the secondary side of the HX is imposed through the time dependent volume 360 at the value of 50 °C and pressure of 1 bar.
The pressure in the gas plenum is maintained at 3 bar in the first part of the transient because of numerical difficulties encountered in the start-up of the simulation. In the second part of the transient there is a decrease to 2 bar that, after the simulations, has been considered large enough to avoid too low pressure in the piping outside the vessel (see Figure 14).
For the initial conditions, the temperatures of all the components were initialized at 270 °C, while for the pressure values the gravimetric distribution with stagnant fluid was assumed.

The main goals of this simulation are the following:

- Analyze the behavior of the facility during the start-up transient until operational conditions are obtained. The analysis will focus on the time trend of some of the parameters in the zones of interest, such as temperature, pressure and flow rate.
- Validate the Froude scaling approach for forced convection.
- Obtain a first thermal-hydraulic characterization of E-SCAPE and consequently of MYRRHA in normal operating conditions.

**Results**

The calculated time trends of LBE mass flow rate through the pumps is shown in Figure 15. Between 0 and 10 s the flow rate increases while the rotation velocity is still zero. From 10 to 100 s the pump flow rate increases linearly, according to the increasing rotational speed. In the next 25 seconds the pump flow rate stabilizes, then it remains constant up to 500 s, when the flow rate has a small increase due to the production of heat power. At this point it achieves...
the nominal value of 25 kg/s. This value remains nearly constant in the remaining phase of the simulation (see Figure 16).

![Graph showing LBE mass flow rate through the pumps in the first part of the transient.]

**Figure 15.** *LBE mass flow rate through the pumps in the first part of the transient.*

![Graph showing LBE mass flow rate through the pumps during the entire transient.]

**Figure 16.** *LBE mass flow rate through the pumps during the entire transient.*

The flow rate through the heater in the first part of the transient is shown in Figure 17. Also here the flow rate increases from 10 to 100 s according to the initial imposed conditions. After...
the 25 seconds during which the pump flow rate and liquid levels stabilize, the heater flow rate reaches the value of 50 kg/s. Again, another little small increase at 550 s is present, when the heater starts to provide the heating power. From 550 s to the end of the transient the conditions do not change (see Figure 18).

Figure 17. LBE mass flow rate in the heater in the first part of the transient.

Figure 18. LBE mass flow rate in the heater during the entire transient.
The flow rate through the heat exchangers follows the outlet flow rate values of the pumps (see Figure 19).

![Graph showing LBE mass flow rate in the heat exchangers.](image)

**Figure 19. LBE mass flow rate in the heat exchangers.**

The LBE temperatures in the inlet and the outlet sections of the heat exchanger (left side) during the first phase of the transient is shown in Figure 20.

It can be noted a transient phase in the first 100 s, when the flow rate is still increasing and the LBE is subjected to heat losses in the piping and heating due to the pump. From 100 to 500 s the trend between inlet and outlet of the HX is the same, and we can find that the heat losses due to convection in the piping makes the temperatures slightly decreasing in time. From 500 s, when the heat power and the heat cooling are provided, we can see a decrease of the HX outlet temperature because cold LBE passes through the HX. After 550 s, the hot LBE starts to arrive from the heater and the temperatures starts to increase throughout the loop with a nearly linear trending time. After 1000 s, the thermal power is maintained constant, but the inlet and outlet temperatures continue to increase with a similar trend until 35000 s, due to the thermal inertia of the eutectic alloy and structures, achieving the stationary value after about 40000 s (Figure 21) due to the inertia of the system.
In Figure 22 we can see the inlet and outlet temperatures in the heater whose trend is similar to the one across the HX. The decrease of temperature in the first part is less pronounced because of mixing with the hotter LBE in the regions above the heat exchangers. We can see that the value of the temperature difference is close to 13.4 °C both for the heater and the heat exchanger.
exchangers; so, the contributions of heat losses and heat power due to the pump are both small and they seem to balance each other. In the Figures it can be also noted that the inlet temperature of the heater seems slightly larger than the outlet temperature of the HX. It allows to check the temperature difference between inlet and outlet section of the components 270-580 equipped with structures exchanging heat with the hot fluid in the upper plenum. In Figure 23 we can see that this contribution is quite small (less than 1 °C) but still present.

Figure 22. LBE temperature upstream and downstream of the heater during the entire transient.

Figure 23. LBE temperature upstream and downstream of the Pipe 270 during the entire transient.
Concerning the heat exchangers secondary side, the temperature time trends are illustrated in Figure 24; the outlet temperature time trend reflects what was observed in the inlet section of HX primary side. In Figure 25 we show the temperatures in the lower plenum and in the pipes 280 and 600: the temperatures, after a first decrease due to the transient stabilization and inertia of the system, achieve a steady state nominal value after 40000 s. We can see that the trend in Pipe 10 and 280, on one side, and 30 and 600, on the other, is very similar confirming the symmetry of the model.

Figure 24. *Water temperature upstream and downstream of the heat exchangers during the entire transient.*
Figure 25. *Pipe 280, pipe 600 and lower plenum temperatures during the entire transient.*

In Figure 25 we can also see that the last volumes of Pipe 10 and Pipe 30 do not reach the values of temperature of the upper volumes. It means that after the initial stabilization transient, the pump jet whose behavior was approximately simulated dividing the lower plenum in three volumes connected by cross flow junction, does not reach the lower volumes. The last three volumes of the lower plenum and pipe 280-600 have the same value of 270 °C that is actually the initialization value: it confirms that the HXs subtract exactly the heat power provides by the heater.

Concerning the hot plenum (components upstream the heater), in Figure 26 we can see that the temperatures in the pipe 70, which represents the barrel, and pipes 100 and 400, which represent the upper plenum, have the same values. Also the temperature in the first volume of pipe 90 which represent the free surface has the value of 283.5 °C. The same is noted for the inlet temperatures of the heat exchangers.

These figures show clearly that the temperature distribution in the plant is mainly characterized by two values: the cold plenum (components upstream the HXs) temperature of about 270 °C and the hot plenum temperature of about 283.5 °C with a temperature difference of 13.5 °C provided by the heater.
Another important thermo-hydraulic property whose distribution in the plant must be considered is pressure; it was monitored at the inlet and the outlet sections of the pumps because the operating point on the pump characteristic curve depends on this value. At the start of the transient pressure decreases in both inlet and outlet sections due to adjustments in stagnant fluid conditions (see Figure 27).
When the flow rate is finally settled at the operating value at about 120 s, the inlet and outlet pressures decrease according to the imposed gas plenum pressure decrease, reaching a nominal value of about 1.45 bar in the inlet section (see Figure 27) and 1.77 bar for the outlet. Furthermore, pressure is also evaluated at the outlet core (pipe 60) and at the inlet core perforated plates (pipe 40). They are important parameters to be taken into account because, as already noted, the pressure drop occurring in the core region determines the level difference between the two free surfaces. The pressure time trend in these volumes is shown in Figure 28.

![Pressure Time Trend](image.png)

**Figure 28. Inlet and outlet pressure in heater.**

The inlet and outlet pressure time trend of the heater is the same mentioned above for the pumps, but the values are different. After a first decrease when the pressure in the gas plenum and the flow rate get the nominal values, pressure in the core reaches the steady state value of 3.45 bar at the inlet and about 2.67 bar at the outlet region. More important for our study is the pressure loss due to the friction, which influences the height of the free surface. The friction contribution is obtained subtracting to the total pressure difference the hydrodynamic contribution due to the weight of LBE.

Figure 29 reports the value of the frictional contribution to pressure drop across the core. It starts from the value of the total pressure difference measured in conditions of static fluid, decreasing until 40 s during the stabilization transient and achieving the nominal value when the flow rate reaches the steady state conditions (after about 120 s) and remains constant until
the end of the simulation. The normal operating value is about 0.33 bar, corresponding to the predictions obtained from the Froude scaling analysis in normal operating conditions.

Figure 29. *Pressure drops due to friction across the core.*

Another important parameter to check is the velocity at the outlet of the core plate. This value in fact has been used to calculate the dimensionless parameters during the scaling analysis. Figure 30 shows the trends of the velocity in junction 65.

Figure 30. *Velocity at the junction 65 (core outlet plate).*
The velocity follows the flow rate trend, achieving the nominal value of 0.34 m/s after about 120 s from the beginning of the transient. This value is less than the 0.38 m/s value, predicted by the scaling analysis: this is due mainly to the different dimensions between the scaled value of total fuel assembly flow area that we considered in scaling analysis for MYRRHA, and the total flow area of the outlet E-SCAPE plate that we used in the calculation, which was referring to an older configuration of MYRRHA. It has been verified that changing this area and adapting the local pressure drop coefficients with the new configuration, the velocity gets the desired value and confirm the validity of the scaling approach.

The last parameters that should be considered in Test A are the liquid levels in components 90, 310 and 640, which represent, respectively, the upper plenum level and the level of the free surface connected to the lower plenum. The goal of this test is to verify that it is possible to obtain nominal conditions, in which the upper plenum level decreases from the initial value and consequently the second free surface level increases, as a consequence of mass conservation. The difference in height between the two levels is determined by the pressure drop in the core following the next relation:

\[ \Delta p_{\text{core}} = \rho g \Delta h_{\text{free surfaces}} \rightarrow \Delta h = \frac{0.33 \cdot 10^5}{9.81 \cdot 10292} = 0.326 \text{m} \]

In Figure 31 we can see that in the first 40 s the two levels have the same value of 0.2 m, with respect to the reference level, \( h_0 = 0 \), assumed at the bottom of pipes 90, 310 and 630, while the total height of these pipes is 0.7 m; The equality of the two levels is obtained because the flow rate in the core is still low to produce a sufficient pressure drop, and then it increases according to the flow rate trend in the core. The difference between the two levels reaches the nominal value after about 40000 s, when the temperature difference and all the other values are stationary as well (see Figure 32). This value is 0.312 m, substantially confirming the prediction; the small difference could be due to different value of LBE density considered in our calculation and RELAP 5 property database.
Figure 31. *Free surface liquid level in the first part of the transient.*

Figure 32. *Free surface liquid level during the entire transient.*
6.2 TEST B

The second considered scenario concerns a loss of heat sink (LOHS) in which the behavior of the entire loop is analyzed after a sudden loss of cooling flow through the secondary side of the HX occurred. The way in which that condition is simulated consists in decreasing rapidly the cooling flow through the secondary side of the HX, acting on the time-dependent junction 360 and 760, after reaching steady state conditions. The accidental transient is considered with a scaled MYRRHA decay heat removal for representing the real plant scenario (Test B1-B2), and with the full 100 kW power (Test B3) for the design of E-SCAPE itself, which needs an indication of the temperatures trend in the plant in case of heat exchanger failure.

6.2.1 Test B1

Boundary conditions

After reaching a steady state, the secondary cooling mass flow rate undergoes a sudden reduction from 40000 to 40060 s and reaches zero for the entire duration of the transient and for both the heat exchangers (symmetric loss of heat sink). In Figure 33, the time trend of the cooling mass flow rate in the secondary side of the HXs is shown, controlled by the components time-dependent junction 360 and 760. In this test the most interesting variables are the inlet and outlet temperatures in the heat exchanger and in the heater.

![Diagram](image)

Figure 33 . Water mass flow rate in the time dependent junctions 360 and 760.
When the incidental transient starts in the MYRRHA reactor, the core is immediately shut down. Consequently in this simulation, when the time dependent junction flows are switched off, the heater follows the scaled MYRRHA decay heat power trend reaching a long term value of 12 kW, as show in Figure 34.

![Figure 34. Heat power trend during the entire transient.](image)

The main goal of this simulation is to analyze the behavior of the MYRRHA reactor during LOHS transient, scaling up from the E-SCAPE results. The analysis will focus on the time trend of some of the key parameters in the zones of interest, such as temperature, flow rate, and $\Delta h_{\text{free surfaces}}$.

The position of the heat exchangers is not important as in the case of natural circulation: it allows to study the MYRRHA incidental transient with the actual configuration design for forced convection.

**Results**

The increase in temperature upstream and downstream the heat exchangers due to the loss of heat sink is shown in Figure 35. It can be noted that during the period in which the loss of cooling in HX (both the heat exchangers) secondary side occurs (around 60 s), the outlet temperature increases until it reaches the value of the inlet section temperature, which is decreasing because of the quick shut down, until the flow rate is completely zero. From this instant on, the trends are almost overlapped and the two temperatures increase linearly until
the end of the transient. This is true for both the heat exchangers, confirming again the symmetry of the model.

The same considerations can be made for the temperatures upstream and downstream the heater (see Figure 36), although the two lines in this case do not exactly overlap due to the thermal power generated in the heater by the decay heat.

Figure 35. Temperature upstream and downstream the primary side of the HXs.

Figure 36. Temperature upstream and downstream the heater.

The temperatures in the upper plenum have the same trend of the heater outlet temperature, while the temperatures in the lower plenum (except for the last volumes which are not reached
from the pump jet) have the same trend of the heater inlet temperature, according to what was seen for normal operating conditions: the plant is in fact characterized by two main values of temperature.

Figure 37 shows the average upper and lower plenum temperature trends.

![Graph showing temperature trends over time](image)

Figure 37. Upper plenum and lower plenum average temperatures.

The LBE mass flow rate in the primary circuit, shown in Figure 38, is affected by the lack of heat removal from the HX. When the accident occurs, the LBE mass flow begins to decrease, due to the increase in temperature. The loss of cooling in the HX also causes a decrease in the LBE density and consequently an increase in the liquid levels on the hydrodynamic components with the presence of a free surface inside of them.

In Figure 39, the liquid level in the components 90, 310 and 640 can be observed; it can be noted that the level increases in all the volumes due to thermal expansion of the liquid metal.
Figure 38. *LBE mass flow rate through the heater.*

Figure 39. *Liquid level in components 90, 310 and 640.*
6.2.2 Test B2

Boundary conditions

The boundary conditions of this test are the same of Test B1, but in this case the loss of sink is asymmetric. It means that only the time dependent junction 760 will be characterized by a decreasing flow rate.

Results

In Figure 40 and Figure 41, the HXs (pipe 240 and pipe 550) and the heater inlet and outlet temperatures trends are shown respectively. It can be observed that the temperatures decrease in both case after the accident occurs. In means that after the shut-down, the cooling capability of one heat exchanger is predicted to be enough to remove the decay heat.

In Figure 42 the LBE flow rate through the heater is shown. It is possible to see how in this case when the accident occurs, the LBE mass flow begins to increase, due to the reducing in temperature.

From the E-SCAPE transient analysis of asymmetric loss of sink, it seems possible to handle this accidental scenario without particular problems.

Figure 40. Temperatures upstream and downstream the primary side of the HXs, left side (a) and right side (b).
Figure 41. *Temperatures upstream and downstream the heater.*

Figure 42. *LBE mass flow rate through the heater.*
6.2.3 Test B3

Boundary conditions
The boundary conditions of this test are the same of Test B1, but in this case, we keep the thermal power to the value of 100 kW for the entire duration of the transient. The main goal of this simulation is to analyze the behavior of the E-SCAPE facility in case of loss of sink keeping a high level of heating power. The system response and in particular the temperature increase rate in the experiment are addressed, considering the “worst case” accident scenario, to foresee the necessary safety measures.

Results
The increase in temperature due to the loss of sink upstream and downstream the heat exchanger (the model is symmetric, consequently we show just the left side heat exchanger) is shown in Figure 43. It can be noted that during the period in which the loss of cooling in HX secondary side occurs (around 60 s), the outlet temperature increases until it reaches the value in the inlet section which, in this case, increases during the first 60 s from the transient beginning. From the instant in which the flow rate in the secondary side is decreased completely down to zero, the trends are almost overlapped and the two temperatures increase linearly in time until the end of the transient.

Figure 43. Temperatures upstream and downstream the primary side of the HX (left side, pipe 280).
The same considerations can be made for the temperatures upstream and downstream the heater (see Figure 44), although also here the two lines in this case do not exactly overlap due to the thermal power generated in the heater. From these figures we can see that after 5000 s from the transient beginning, the temperature increase is about 270 °C: this leaves enough time for the safety systems actions.

![Figure 44. Temperatures upstream and downstream the heater.](image)

In Figure 45 is shown the LBE flow rate through the heater which also in this case is decreasing because of the temperature increase.

![Figure 45. LBE mass flow rate through the heater.](image)
6.3 TEST C

This test represents the third accident scenario in which a sudden failure of the main pumps is assumed. To obtain this particular condition, the pump rotational speed is decreased to zero in a few seconds, with a consequent decrease of the LBE mass flow rate. In order to have a full analysis of the plant behavior, the pump failure is considered with the MYRRHA scaled decay heat removal model. The simulation has been performed on the simplified scaled model of MYRRHA reactor with the HXs inside the vessel.

**Boundary conditions**

The pumps are turned off at 19000 s with a stopping time of 10 s following a prescribed coast-down curve (see Figure 46) and then are kept switched off in order to evaluate changes in temperature of the liquid metal in the circuit while the heat power is being shut down, thus inducing decay heat removal in the heater zone. Figure 47 and Figure 48 show how the transient is actually simulated.

![Main pumps coast-down curve](image)

*Figure 46. Main pumps coast-down curve.*
The main goals of this simulation are the following:

- To analyze the behavior of the MYRRHA reactor during LOCA transient, scaling up from the E-SCAPE results. The analysis will focus on a first estimation of E-SCAPE natural circulation capabilities, providing a consequent estimation of MYRRHA capabilities as well.
• To validate the scaling approach prediction for such conditions.

Results

Figure 49 shows the time evolution of the LBE mass flow rate. It shows that the flow decreases abruptly after pump failure. After 8000 s from the transient beginning, we can admit that the decay heat removal remains quasi-stationary: the analytic solution found for the MYRRHA reactor natural circulation evaluation refers to 8000 s from the core shut-down as well. Thus, we can check the agreement between the scaled stationary value prediction of 1.77 kg/s and the RELAP 5 simulation of the scaled MYRRHA model after 8000 s from the transient beginning (27000 s from the start of the simulation).

Because of the very small time step used in the simulation and consequently the long calculation time needed, we stopped the calculation after 27320 s, after achieving the stationary value and when the decay heat removal is already constant.

In Figure 49 we can note that after 8000 s from the pump failure, the value of the heater flow rate oscillates between the 1.2 kg/s and 3.2 kg/s with a medium value of about 2.2 kg/s. The strong oscillations are due to the fact that the buoyancy contribution is very low, so the driving force in the heater region seems to be not enough for a stable natural convection circulation.
Comparing the predicted value from the scaling analysis and the value obtained from the simulation and considering the approximations utilized for a preliminary study, it seems reasonable to consider the scaling approach adopted for the natural circulation capability of MYRRHA coherent with the obtained RELAP5 results. It seems thus also reasonable to review the initial E-SCAPE design and carry out a new design in which the heat exchangers are put outside the vessel (practical reasons) but with an height difference between core and HX middle planes coherent with the scaled MYRRHA version studied above (0.16 m).

![Graph showing temperature upstream and downstream the heater.](image)

**Figure 50. Temperature upstream and downstream the heater.**

In Figure 50 the temperature difference across the heater is shown. We can see that the value after 8000 s from the transient start is around 33 °C.
Chapter 7. Conclusions

In this thesis, a first thermal-hydraulic assessment of the operating conditions of the liquid metal pool experiment E-SCAPE was performed. In particular a scaling methodology to be applied to the MYRRHA reactor to obtain the main parameters of the experimental facility has been proposed for both normal operating conditions and incidental transients with natural circulation and decay heat removal.

The scaling analysis revealed that for normal operating conditions the Froude number preserving criterion seems to give the most appropriate representation of the real plant behavior, without raising particular problems from the engineering design and operating points of view. The maximum velocity preservation criterion, on the other hand, seems to be less suitable to study free surface appearance and it results in a too large value of free surface levels difference. Moreover, the Reynolds number preservation provides LBE velocities which cannot be achieved in practice and consequently has been discarded.

The choice of the length scaling factor was based on the principle of optimizing most of the non dimensional numbers of the three identified groups, while considering also economic feasibility of the apparatus. As a matter of fact, economic considerations pushed towards smaller scales, while practical considerations demanded for larger scales. For the E-SCAPE facility, a vessel diameter of 1.2 m was considered optimal in this respect, corresponding to a geometrical scaling factor of 1/6.3. With this choice it was demonstrated that the Reynolds number at the exit of the core outlet plate was still large enough to keep turbulent flow conditions.

The first conceptual design of the experimental facility was set up for studying normal operating conditions. During the work, it was therefore decided to study the possibility to deal also with natural convection conditions without changing completely the design, already in advanced state of development. For this purpose and in order to reproduce also proper mixing from buoyant jets, it was decided to adopt the Richardson number preservation criterion, which ensures the preservation of the Froude number as well. To perform the scaling analysis in natural circulation, it was necessary to first estimate the MYRRHA natural circulation capabilities in steady-state conditions with a simplified analytical model. From this evaluation and from the set of equations obtained with the scaling analysis, a very preliminary characterization of E-SCAPE natural circulation has been perform which will be verified in comparison with the experimental results.
To validate the scaling analysis and to obtain a first thermal-hydraulic characterization of the experimental facility, a thermal hydraulic analysis with the system code RELAP 5 has been performed, considering two different heat exchanger positions. The RELAP5 nodalization, developed following the first SCK-CEN design proposal and the first scaling results, was the result of a difficult process of optimization under different constraints related to each other, like free surface levels, pressure drops and flow rate in the heater, requiring the singular pressure loss coefficient determination in that zone. The model can be indeed intended an idealization of the real plant, also considering the difficulties to simulate a pool type reactor with a one dimensional system code.

The results obtained in the predictions by the RELAP5 code of the start-up and steady state scenarios and of the main accidental transients for the MYRRHA reactor and the E-SCAPE facility provide important preliminary information about the thermal-hydraulic behavior of the facility concerning the LBE temperature in the core and in the HX, the liquid levels inside the pool, the flow rate due to natural circulation conditions and other relevant aspects.

The analysis of the start-up transient (Test A) shows that the difference in the levels of the two free surfaces is actually determined by the pressure drop in the core, which amounts at roughly 0.33 bar. The liquid metal velocity at the outlet of the heater plate is about 0.34 m/s while the LBE mass flow rate in the heater reaches the nominal value of 50 kg/s in about 550 s. This analysis also allowed to verify the correct removal of the heat generated in the heater by the foreseen HXs.

Concerning the temperatures, from the simulations it appeared clear that the facility will be characterized by two main steady-state values: a hot plenum temperature of about 283 °C and a cold plenum temperature of about 270°C. In the MYRRHA reactor a by-pass flow rate is foreseen and the temperature difference between upper and lower plenum will be thus different from the core temperature difference. In the future it will be necessary to foresee a by-pass flow rate also in E-SCAPE, to better simulate the MYRRHA temperature pattern.

The mixing phenomena in the lower plenum, difficult to simulate with a one dimensional system code, are not supposed to be simulated in a completely reliable way, but with the nodalization developed it was at least possible to extend the temperature mixing until the last two volumes at the bottom of the lower plenum, in qualitative agreement with what expected to occur in the real conditions. In the future anyway, these phenomena need to be studied in detail with CFD calculations.
In the case of symmetric loss of heat sink (LOHS) with decay heat removal (Test B1) the mass flow rate in the primary loop decreases and the heater temperature increases with a sufficiently slow rate. From a MYRRHA safety point of view, scaling up the results for E-SCAPE, we can aspect that the time constants involved in the dynamics of the transient will allow the automatic control system or operators to have a sufficient time margin to drive the system towards safe conditions.

In the case of asymmetric loss of heat sink (LOHS) with decay heat removal (Test B2) the heater temperature decreases and consequently the mass flow rate increases. Again, from a MYRRHA safety point of view the cooling capability of one HX seems to be enough to control the accident scenario without the need of additional devices.

The analysis of the symmetric loss of heat sink (LOHS) keeping the 100 kW heat power (Test B3) had the goal to evaluate the system response in case of an accident scenario in view of the safety assessment of E-SCAPE itself. The heater temperature trend shows that in 5000 s the temperature increases of about 270 °C, leaving enough time to switch off the heater without particular difficulties.

The last transient scenario studied was the symmetric pump failure which allowed to study natural circulation. In this purpose, a model with the heat exchangers located inside the pool was considered because the previously developed scaling approach suggested a proper scaling between lengths. The results show that the flow rate value is quite close to the value estimated from scaling.

In summary, the obtained RELAP 5 results confirm the validity of the scaling approach for forced convection flow and provided a first thermal-hydraulic characterization of the E-SCAPE facility. The correctness of the approach for the exact simulation of natural circulation still needs further investigation and confirmation, but it seems to reasonably suggest to change the first lay-out of the facility, reducing the height of the heat exchangers with respect to the heated region.
Bibliography


