State-of-the-Art Report on Molten Corium Concrete Interaction and Ex-Vessel Molten Core Coolability
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Foreword

Since 2002, the Nuclear Energy Agency (NEA) has sponsored the Melt Coolability and Concrete Interaction (MCCI) co-operative project in two phases in order to investigate ex-vessel melt coolability and concrete interaction by means of separate-effects tests and large-scale integral tests carried out at the Argonne National Laboratory (ANL). The second phase of the experimental programme was completed in March 2010. Key elements of this programme included the conduct of experiments involving real reactor material and associated analyses with the objectives of resolving the ex-vessel debris coolability issues and addressing remaining uncertainties related to long-term two-dimensional molten core-concrete interactions under both wet and dry cavity conditions. It was expected that the achievement of these two objectives would demonstrate the efficacy of severe accident management guidelines for existing plants and provide the technical basis for better containment designs for the future plants. During both phases of the programme, a total of 13 separate effects tests were conducted to provide data on various core debris cooling mechanisms, including two specific tests to study mechanisms connected to new design features to enhance coolability by bottom water injection. In addition, six large-scale integral tests were conducted to provide data on long-term two-dimensional core-concrete interaction under both wet and dry cavity conditions and one specific one-dimensional large scale test was conducted to investigate the approach for augmenting ex-vessel corium coolability based on an externally-cooled surface concept. These tests provided a broad database to support the development and validation of models and codes with an aim to assess the behaviour of the full size plants under given conditions.

The Committee on the Safety of Nuclear Installations (CSNI) held a seminar in November 2010 where the major outcomes of the MCCI Project and other complementary experimental activities were presented and discussed.

One of the recommendations of this wrap-up seminar was: “The preparation of a state-of-the-art report on melt coolability and core concrete interactions that captures the last thirty years of international research results”. A consensus was reached to start with the preparation of the recommended report. A proposal was discussed within the framework of the Working Group on Risk Assessment (WGAMA) and endorsed by the CSNI in late 2011.

The mandate and the need for a comprehensive state-of-the-art report (SOAR) can be illustrated by the number of questions raised and discussed during MCCI Project and WGAMA meetings. These questions are:

- What can be learnt from the experimental data base including the last results of the MCCI Project and complementary national or European projects?
- What progress has been made and what is the level of remaining uncertainties on the modelling of corium concrete interaction and molten core coolability?
- What could be concluded about the capabilities of the codes to predict corium concrete interaction and molten core coolability with respect to containment integrity assessment in plant application?
What are the remaining issues and opportunities to define further experimental or analytical activities?

To address these questions, a working group was set up and a kick-off meeting took place in April 2012.

After five working meetings, the present state-of-the-art report is the result of the work of a group which was co-ordinated by Jean-Michel Bonnet (IRSN, France) and included representatives from Japan, Italy, France, Germany, Russia, Spain and the United States. The technical secretariat was carried out by Martin Kissane (Nuclear Energy Agency).

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Within the realm of past work, several synthesis papers exist in the open literature but only one state-of-the-art report (SOAR) was published in 1995 (Alsmeyer H., et al., 1995). This 1995 SOAR was prepared in the framework of the specific European programme, ”NUCLEAR Fission Safety 1990-1994”, of the European Atomic Energy Community Reinforced Concerted Action on Reactor Safety.

The MCCI SOAR working group also acknowledges the benefit of a European activity conducted in parallel that aimed to write a report on knowledge improvement based on the work performed on corium concrete interaction between 2004 and 2012 under the framework of SARNET.
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### List of abbreviations and acronyms

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<tr>
<td>ABI</td>
<td>Automated ball indentation</td>
</tr>
<tr>
<td>ABWR</td>
<td>Advanced boiling-water reactor</td>
</tr>
<tr>
<td>AC</td>
<td>Alternating current</td>
</tr>
<tr>
<td>ACE</td>
<td>Advanced containment experiments</td>
</tr>
<tr>
<td>ACIWA</td>
<td>AC-independent water addition</td>
</tr>
<tr>
<td>ADS</td>
<td>Automatic depressurisation system</td>
</tr>
<tr>
<td>AEG</td>
<td>Atomic Energy Act</td>
</tr>
<tr>
<td>AES91</td>
<td>A specific VVER (or WWER) design</td>
</tr>
<tr>
<td>AFNOR</td>
<td>Association française de normalisation</td>
</tr>
<tr>
<td>AFW</td>
<td>Auxiliary feed water</td>
</tr>
<tr>
<td>ALWR</td>
<td>Advanced light-water reactor</td>
</tr>
<tr>
<td>ANL</td>
<td>Argonne National Laboratory (United States)</td>
</tr>
<tr>
<td>APWR</td>
<td>Advanced pressurised water reactor</td>
</tr>
<tr>
<td>ASTEC</td>
<td>Accident source term evaluation code</td>
</tr>
<tr>
<td>ASN</td>
<td>Autorité de sûreté nucléaire (France)</td>
</tr>
<tr>
<td>AtG</td>
<td>Atomic Energy Act</td>
</tr>
<tr>
<td>BETA</td>
<td>A large scale test facility to study melt concrete interaction in a cylindrical concrete crucible</td>
</tr>
<tr>
<td>BiMAC</td>
<td>Basemat internal melt arrest and coolability</td>
</tr>
<tr>
<td>BIP</td>
<td>Behaviour of Iodine Project</td>
</tr>
<tr>
<td>BSAF</td>
<td>Benchmark Study of the Accident at the Fukushima Daiichi Nuclear Power Plant</td>
</tr>
<tr>
<td>BWR</td>
<td>Boiling water reactor</td>
</tr>
<tr>
<td>BWRS</td>
<td>Boiling water reactor stability</td>
</tr>
<tr>
<td>CANDU</td>
<td>Canadian deuterium uranium reactor</td>
</tr>
<tr>
<td>CAV</td>
<td>Cavity package</td>
</tr>
<tr>
<td>CCI</td>
<td>Core concrete interaction</td>
</tr>
<tr>
<td>CEA</td>
<td>Commissariat à l’énergie atomique et aux énergies alternatives (France)</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
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<tr>
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</tr>
<tr>
<td>CFD</td>
<td>Computational fluid dynamics</td>
</tr>
<tr>
<td>CFR</td>
<td>Code of Federal Regulations</td>
</tr>
<tr>
<td>CHF</td>
<td>Critical heat flux</td>
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<tr>
<td>CHRS</td>
<td>Containment heat removal system</td>
</tr>
<tr>
<td>CIS</td>
<td>Commonwealth of Independent States</td>
</tr>
<tr>
<td>CLN</td>
<td>Coolant</td>
</tr>
<tr>
<td>CMSS</td>
<td>Core melt stabilisation system</td>
</tr>
<tr>
<td>CNRA</td>
<td>Committee on Nuclear Regulatory Activities</td>
</tr>
<tr>
<td>COCO</td>
<td>3D Core calculation code</td>
</tr>
<tr>
<td>COLIMA</td>
<td>Corium liquid and materials</td>
</tr>
<tr>
<td>COMET</td>
<td>NEA computer code program</td>
</tr>
<tr>
<td>COPS</td>
<td>Containment overpressure protection system</td>
</tr>
<tr>
<td>CORCON</td>
<td>NEA computer program (Mechanical and Thermal Stress in Fuel Element Clad)</td>
</tr>
<tr>
<td>CORPRO</td>
<td>Corium properties database</td>
</tr>
<tr>
<td>COSACO</td>
<td>Areva computer code for melt coolability and concrete interaction studies</td>
</tr>
<tr>
<td>COSCHEM</td>
<td>A real-solution chemical-substance database</td>
</tr>
<tr>
<td>COTES</td>
<td>Computer code</td>
</tr>
<tr>
<td>CPU</td>
<td>Central processing unit</td>
</tr>
<tr>
<td>CRC</td>
<td>CRC Press</td>
</tr>
<tr>
<td>CRD</td>
<td>Control rod drive</td>
</tr>
<tr>
<td>CSE</td>
<td>Complementary safety evaluations</td>
</tr>
<tr>
<td>CSNI</td>
<td>Committee on the Safety of Nuclear Installations</td>
</tr>
<tr>
<td>CVH</td>
<td>Control volume hydrodynamics</td>
</tr>
<tr>
<td>DEH</td>
<td>Direct electrical heating</td>
</tr>
<tr>
<td>DF</td>
<td>Decontamination factor</td>
</tr>
<tr>
<td>DiD</td>
<td>Defence in depth</td>
</tr>
<tr>
<td>DTA</td>
<td>Differential thermal analysis</td>
</tr>
<tr>
<td>DVI</td>
<td>Direct vessel injection</td>
</tr>
<tr>
<td>EC</td>
<td>European Commission</td>
</tr>
<tr>
<td>ECOKATS</td>
<td>Name of an experimentation within the European Union project ECOSTAR</td>
</tr>
<tr>
<td>ECOSTAR</td>
<td>European Union project</td>
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<tr>
<td>EDF</td>
<td>Électricité de France</td>
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<tr>
<td>EFCVS</td>
<td>Emergency filtered containment venting system</td>
</tr>
<tr>
<td>EMF</td>
<td>Electric melt furnace</td>
</tr>
<tr>
<td>Acronym</td>
<td>Description</td>
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<td>-----------------------------------------------------------------------------</td>
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<tr>
<td>ENSREG</td>
<td>European Nuclear Safety Regulators Group</td>
</tr>
<tr>
<td>EOC</td>
<td>Effective orthotropic heat transfer coefficients</td>
</tr>
<tr>
<td>EOP</td>
<td>Emergency operating procedures</td>
</tr>
<tr>
<td>EPR</td>
<td>European pressurised reactor</td>
</tr>
<tr>
<td>EPRI</td>
<td>Electric Power Research Institute</td>
</tr>
<tr>
<td>ERI</td>
<td>EURSAFE research issues</td>
</tr>
<tr>
<td>ERMSAR</td>
<td>European review meeting on severe accident research</td>
</tr>
<tr>
<td>ESBWR</td>
<td>Economic Simplified Boiling Water Reactor</td>
</tr>
<tr>
<td>ESF</td>
<td>Engineered safety features</td>
</tr>
<tr>
<td>EUR</td>
<td>European utility requirements</td>
</tr>
<tr>
<td>EURSAFE</td>
<td>European expert network for the reduction of uncertainties in severe accident safety issues</td>
</tr>
<tr>
<td>FCI</td>
<td>Fuel-coolant interaction</td>
</tr>
<tr>
<td>FCHF</td>
<td>Corium-pool-to-water heat flux (Modular accident analysis program code)</td>
</tr>
<tr>
<td>FE</td>
<td>Finite element</td>
</tr>
<tr>
<td>FEM</td>
<td>Finite element method</td>
</tr>
<tr>
<td>FINCCI</td>
<td>Computer code</td>
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<tr>
<td>FRAG</td>
<td>Computer code</td>
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<tr>
<td>FZK</td>
<td>Forschungszentrum Karlsruhe (Germany)</td>
</tr>
<tr>
<td>GAREC</td>
<td>Computer code</td>
</tr>
<tr>
<td>GDCS</td>
<td>Gravity-driven cooling system</td>
</tr>
<tr>
<td>GEMINI</td>
<td>Gibbs Energy MINImiser (software for thermo-chemical equilibrium solver)</td>
</tr>
<tr>
<td>HECLA</td>
<td>Computer code</td>
</tr>
<tr>
<td>HEFEST</td>
<td>Computer code</td>
</tr>
<tr>
<td>HMX</td>
<td>Heavy oxides and metals</td>
</tr>
<tr>
<td>HOX</td>
<td>Oxide phase</td>
</tr>
<tr>
<td>HPCI</td>
<td>High pressure core injection</td>
</tr>
<tr>
<td>HSS</td>
<td>Heated solid slug of steel</td>
</tr>
<tr>
<td>INRNE</td>
<td>Institute for Nuclear Research and Nuclear Energy (Bulgaria)</td>
</tr>
<tr>
<td>IRSN</td>
<td>Institut de radioprotection et de sûreté nucléaire (France)</td>
</tr>
<tr>
<td>IAEA</td>
<td>International Atomic Energy Agency</td>
</tr>
<tr>
<td>ICI</td>
<td>In-core instrument</td>
</tr>
<tr>
<td>IRWST</td>
<td>Internal refuelling water storage tank</td>
</tr>
<tr>
<td>KEPCO</td>
<td>Korea Electric Power Industry</td>
</tr>
<tr>
<td>KIT</td>
<td>Karlsruhe Institute of Technology (Germany)</td>
</tr>
</tbody>
</table>
LBLOCA  Large break loss-of-coolant accident
LCS    Limestone-common sand
LDF    Lower drywell flooder
LOCA   Loss-of-coolant accident
LOX    Light oxide phase
LPCI   Low-pressure core injection
LWR    Light water reactor
MACE   Melt attack and coolability experiments
MAAP   Modular accident analysis program
MBDBE  Mitigation of beyond-design-basis events
MCCI   Melt coolability and concrete interaction
MELCOR Methods for estimation of leakages and consequences of releases
MOCKA  German experimental facility for melt coolability and concrete interaction studies
NEA    Nuclear Energy Agency
NPP    Nuclear power plant
NRA    Nuclear Regulation Authority
NRC    Nuclear Regulatory Commission
NUCLEA French (IRSN) thermodynamic database for nuclear applications
OECD   Organisation for Economic Co-operation and Development
OSSA   Operating strategies for severe accidents
PAR    Passive autocatalytic recombiners
PIRT   Phenomena identification and ranking table
PLE    Plant lifetime extension
PLINIUS French (CEA) experimental platform for corium studies
PRECOS Russian experimental programme that characterised ceramics relevant to in-vessel and ex-vessel corium
PRHR   Passive residual-heat removal
PSA    Probabilistic safety analysis
PSR    Periodic safety reviews
PWR    Pressurised water reactors
RCIC   Reactor core isolation cooling
RHWG   Working group on reactor harmonisation
RPV    Reactor pressure vessel
RWSP   Reactor water storage pit
<table>
<thead>
<tr>
<th>Acronym</th>
<th>Description</th>
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<tbody>
<tr>
<td>SAHRS</td>
<td>Severe accident Heat removal system</td>
</tr>
<tr>
<td>SAM</td>
<td>Severe accident management</td>
</tr>
<tr>
<td>SAMG</td>
<td>Severe accident management guideline</td>
</tr>
<tr>
<td>SAMIME</td>
<td>Severe accident management implementation and expertise in the European Union</td>
</tr>
<tr>
<td>SAREF</td>
<td>Safety research opportunities post-Fukushima</td>
</tr>
<tr>
<td>SARP</td>
<td>Severe accident research priority</td>
</tr>
<tr>
<td>SESAM</td>
<td>Senior Group of Experts on Severe Accident Management</td>
</tr>
<tr>
<td>SNL</td>
<td>Sandia National Laboratories (United States)</td>
</tr>
<tr>
<td>SOAR</td>
<td>State-of-the-art report</td>
</tr>
<tr>
<td>SOARCA</td>
<td>State-of-the-art reactor consequence analysis</td>
</tr>
<tr>
<td>SOCRAT</td>
<td>Russian severe-accident computer code</td>
</tr>
<tr>
<td>SSC</td>
<td>Safety systems and components</td>
</tr>
<tr>
<td>STCP</td>
<td>Source term code package</td>
</tr>
<tr>
<td>SURC</td>
<td>Sustained urania concrete</td>
</tr>
<tr>
<td>TOLBIAC</td>
<td>French (CEA) computer code for analysis of melt coolability and concrete interaction</td>
</tr>
<tr>
<td>TOP</td>
<td>Technical opinion paper</td>
</tr>
<tr>
<td>TSO</td>
<td>Technical Safety Organisations</td>
</tr>
<tr>
<td>WENRA</td>
<td>Western European Nuclear Regulators’ Association</td>
</tr>
<tr>
<td>WEX</td>
<td>German developed computer code for MCCI analysis of melt coolability and concrete interaction</td>
</tr>
<tr>
<td>WWER</td>
<td>Vodo-Vodyanoi Energetichesky Reaktor, a Russian-designed pressurised water reactor (also sometimes referred to as WWER)</td>
</tr>
<tr>
<td>WGAMA</td>
<td>Working Group on Accident Management and Analysis</td>
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</table>
Executive summary

Since 2002, the Nuclear Energy Agency (NEA) has sponsored the Melt Coolability and Concrete Interactions (MCCI) co-operative project in two phases in order to investigate ex-vessel melt coolability and concrete interaction by means of separate-effects tests and large scale integral tests carried out at the Argonne National Laboratory (ANL). Key elements of this project included the conduct of experiments involving real reactor material and associated analyses with the objectives of resolving the ex-vessel debris\(^1\) coolability issue and addressing remaining uncertainties related to long-term two-dimensional molten core-concrete interactions under both wet and dry cavity conditions. It was expected that the achievement of these two objectives would demonstrate the efficacy of severe accident management guidelines for existing plants and provide the technical basis for better containment designs for future plants.

Six months after the second phase of the experimental programme was completed, the Committee on the Safety of Nuclear Installations (CSNI) held a seminar in November 2010 where the major outcomes of the MCCI Project and other complementary experimental activities were presented and discussed. One of the recommendations from the seminar was: “The preparation of a state-of-the-art report (SOAR) on melt coolability and core concrete interactions that captures the last thirty years of international research results”. A working group was established in April 2012 within the framework of the Working Group on Accident Management and Analysis (WGAMA) to address this recommendation.

The SOAR provides a background discussion of safety issues relevant to core-concrete interactions and melt coolability and related containment failure modes, an overview of various experiment programmes that have been carried out in the areas of molten-corium-concrete interaction and debris coolability, a description and assessment of various analytical tools (“codes”) that have been developed to analyse MCCI behaviour, and finally, a summary of plant analysis activities that have been carried out using these codes. These various activities, carried out over the last three decades, have significantly increased our level of understanding regarding MCCI behaviour under both wet and dry cavity conditions. Depending upon containment design, regulatory requirements, and accident management considerations that are unique to each country and reactor type, the current level of understanding in this area is sufficient for conservative reactor safety assessments. However, a few areas have been identified (particularly based on lessons learnt from Fukushima Daiichi) that may warrant further investigation to reduce residual uncertainties.

While existing data and experiments indicate that debris coolability can be achieved within an envelope that is principally based on concrete type, melt depth, and timing of cavity flooding, this envelope does not encompass the full range of accident conditions that can be encountered in certain plant configurations. Neither does the envelope encompass various abstractions of melt progression in-vessel which give rise to different initial and boundary conditions for ex-vessel melt progression and also, wide variations in concrete constituents within the two major types investigated in the experimental programme and consequent effects of such variations. Also, the report focuses on the

---

1. The term “debris” refers to the corium melt in general and not only to the solid particles.
progress made in the last two decades on the thermal-hydraulic aspects of MCCI and mentions in passing some early research programmes dealing with fission products aspects of MCCI. Finally, the report discusses general aspects of severe accident management strategies aimed at achieving melt stabilisation in both generation II and generation III reactors.

Lessons learnt from experiments

The general goals of the MCCI experiments under both dry cavity (i.e. without mitigation measures involving water addition) and wet cavity (i.e. with mitigation measures involving water addition) have been to: i) identify and characterise important phenomenological processes in order to facilitate model development, and ii) provide experimental data to support validation of models and codes that are used in reactor safety assessments. For dry cavity conditions, the research focused on evaluating the nature and extent of core-concrete interaction, basemat and sidewall erosion, and concurrent fission product release. For wet cavity conditions, the research focused on evaluating the effectiveness of water in terminating the MCCI by quenching the molten core material and rendering it permanently coolable, i.e. to achieve the melt stabilisation condition.

The various accident sequences and the possibility of operator intervention result in a broad range of possible initial conditions at time of vessel failure. Following the accident at Three Mile Island and some studies of melt interactions with concrete, it was presumed that core degradation would be very heterogeneous with central regions of the core melting while peripheral regions were barely degraded. Additional core materials would cascade for protracted periods from the reactor vessel as core debris attacked concrete. A certain fraction of the cladding would not be oxidised at the time of core debris relocation to the lower head of the pressure vessel and upon vessel breach there would be a chemical component to the heat generation in the core debris. Additionally, the state of knowledge about late in-vessel melt progression is incomplete (particularly for boiling water reactors, BWRs). Thus, there is considerable uncertainty regarding the MCCI initial conditions that includes the timing of reactor pressure vessel (RPV) failure; the initial temperature, mass, and composition of the core debris; the possibility of segregation of metal and oxide melt phases; the pour rate of the melt from the RPV that is determined principally by the melting rate of residual core material, and to a lesser extent by the opening in the RPV lower head; and finally, the timing of water injection (if any).

Many of these parameters (e.g. power level in the ex-vessel core debris, which is indicative of the time of vessel failure, as well as melt mass and composition) have been addressed in various experimental programmes that are described in Chapter 2. It is important to recognise that as the understanding of core degradation evolved since the Three Mile Island accident and now continues to evolve since the Fukushima accident, modelling of in-vessel melt progression likewise will continue to evolve. As such, no attempt has been made in this report to encompass the full range of possible initial and boundary conditions (some of which are known at present) and to conclude that the current understanding of the MCCI phenomena is complete.

The various test series described in Chapter 2 investigated the effects of melt composition, concrete type, input power to melt, and in experiments with cavity flooding (wet cavity experiments), the timing of water addition on two-dimensional core-concrete interaction and melt coolability. Principal variables measured during the experiments included melt temperature and local concrete ablation rates. For flooded cavity experiments, the debris/water heat flux after cavity flooding was also estimated based on the rate of steam production from the interaction. Key observations from these tests are summarised below.

Under dry cavity conditions, all tests exhibit the overall trend of decreasing melt temperature as concrete ablation progresses and increasing heat transfer surface area as the melt expands into the concrete. This trend depends also on the decrease of the interface temperature between the melt and the upper crust as the melt becomes enriched with low melting concrete decomposition products. The
results from several reactor material experiments indicate that the concrete ablation process for oxidic core melts is influenced by concrete types. For limestone-common sand concrete, the radial to axial erosion rate and ablation depth are approximately one to one whereas for siliceous concrete, it is approximately three to one. To investigate whether the relatively small melt pool aspect ratio (i.e. test section width/melt depth) used in the experiments has an influence on the radial/axial power split observed in the dry cavity experiments, a dedicated large scale experiment was carried out. The results indicate that an increase in aspect ratio from approximately 1 (typical of most reactor material tests) to about 3.2 has no noticeable effect on the ablation characteristics for siliceous concrete. This observation lends additional credibility to the measured erosion rates and ablation depths in various experiments. It is noted that the forensic examinations of Chernobyl Unit 4 are consistent with the experiment observations for siliceous concrete thus giving credibility to long term extrapolation of experimental data even if the ablation asymmetry is not yet understood from a mechanistic point of view.

Post-test examinations have shown that the nature of the core-concrete interface is noticeably different for limestone tests in comparison to siliceous tests. The differences in interface characteristics may influence the heat transfer at the interface, yielding different concrete ablation behaviour for different concrete types. The overall trend in the ablation front progression that has been observed under experiment as well as Chernobyl Unit 4 examinations cannot, however, be explained fully on the basis of our current understanding of the ablation behaviour and modelling of such behaviour. Thus, extrapolation of the results to plant conditions remains somewhat uncertain due to the lack of a more robust phenomenological model that can rationalise the differences in the observed cavity erosion behaviour of the two concrete types used in the experiments. It is worth mentioning that while variations of the two major concrete types (e.g. representative concrete type in French plants with variations of siliceous aggregates and “serpentine” concrete used in one of the Argonne Core-Concrete Experiments [ACE]) were used in some experiments, the database of such variations is somewhat limited.

Several experiments have provided evidence that initial corium crust formation on cold concrete surfaces can influence the early (tens of minutes to an hour) core-concrete interaction behaviour. During this phase, basemat ablation is minimal and melt temperature remains high due to the insulating effect of the crusts. This effect has been observed in both transient as well as sustained heating reactor material tests. However, once crusts at concrete interface fail, concrete ablation proceeds vigorously and the melt temperature declines due to the above mentioned effects. Although the data are not conclusive, there is evidence that gas evolution from concrete decomposition can act to destabilise these interfacial crusts.

The effect of unoxidised Zr cladding on the thermal-hydraulics of the core-concrete interaction was investigated in several experiment programmes. The oxidation reaction between Zr and sparging concrete decomposition gases (CO₂, H₂O) caused exothermic transients during which the melt temperatures increased over a period of tens of minutes in the experiments. This transient behaviour was observed in both reactor material as well as simulant experimental tests. The data further indicate that, after the cladding is fully oxidised, melt temperature drops to a value consistent with fully oxidised melt conditions. Aside from cladding oxidation effects, a limited number of experiments have been conducted to examine the effect of significant structural steel² content in the melt on core-concrete interaction behaviour. One outcome from these tests is the extensive amount of iron oxidation that occurs with limestone-common sand concrete. Steel oxidation also occurred in tests with siliceous concrete, but to a lesser extent in comparison to the limestone case. Concrete temperatures showed

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2. Steel comes from in-vessel structures or rebars embedded in the concrete for its reinforcement
that axial and radial ablations were more pronounced in the areas where metal was found. This is consistent with other oxide-metal simulant tests that have shown enhanced heat transfer at the metal-concrete interface relative to the oxide-concrete interface. Notwithstanding these findings, the metal-oxide database is noted to be limited and there is no clear understanding of the phenomenological behaviour for this case (i.e. mixed vs. stratified metal-oxide conditions, and/or bifurcation between these two states during the interaction). Therefore, extrapolation of experimental results obtained thus far to plant conditions is somewhat uncertain.

The fission product release during core/concrete interaction is only briefly mentioned in the report as it has not been the main focus of the MCCI experiments in the last two decades. The aerosols released during corium/concrete interaction contain mainly elements from the concrete. The release of uranium or low-volatile fission product is enhanced by the presence of metal in the melt and by the higher gas content of limestone common sand concrete but remains low. Interaction with silicon to form silicates tends to lower the release of fission products of main interest like barium and strontium.

Regarding debris coolability, the test series provided evidence of several heat transfer mechanisms that can contribute to long-term corium cooling. When the core debris is flooded from above, the question of whether or not a significant amount of the thermal energy will initially be removed depends upon whether a stable crust is able to form that inhibits heat transfer from the melt to the water layer. For a stable crust to form, two conditions must be met: (i) a thermal condition, viz., the melt/water interfacial temperature must fall below the corium freezing temperature, and (ii) a mechanical condition, viz., the incipient crust must be stable with respect to local mechanical loads imposed by the agitated melt. In this bulk cooling regime, efficient melt/water heat transfer occurs predominately by radiation heat transfer across the agitated (i.e. area enhanced) melt/water interface, in addition to entrainment of melt droplets into the overlying water and conduction.

As bulk cooling heat transfer continues, the melt temperature gradually declines. As the downward heat transfer rate decreases, then melt sparging arising from concrete decomposition also decreases. Thus, a point is eventually reached at which the thermal and mechanical thresholds for interfacial crust formation are both satisfied, and an insulating crust forms between the coherent melt zone and the water layer. This crust is characterised by some degree of porosity, or cracks, owing to the necessity of venting concrete decomposition gases.

After the crust forms, completion of the quench process can only be achieved if water is able to penetrate into the debris by some mechanism to provide sufficient augmentation to the otherwise conduction-limited heat transfer process to remove the decay heat. The tests have revealed three mechanisms that provide pathways for water to penetrate the debris. The first is water ingestion through interconnected porosity or cracks. The second mechanism is particle bed formation through “volcanic” eruptions. In this case, concrete decomposition gases entrain melt droplets into the overlying coolant as they pass through the crust. The third mechanism is mechanical breach of a suspended crust. In particular, the thick crusts that form from water ingestion could bond to the reactor cavity walls, eventually causing the melt to separate from the crust as the MCCI continues downwards. However, this “anchored” or suspended configuration is not expected to be mechanically stable at reactor scale due to the low mechanical strength of the crust. The suspended crust situation is somewhat different from a supported crust situation as in many magma flow from volcanic eruptions. Eventually the suspended crust is expected to fail, leading to rapid and massive ingestion of water beneath the crust. This sudden introduction of water provides a pathway for renewed debris cooling by the bulk cooling, water ingestion, and melt eruption cooling mechanisms.

Several findings related to debris coolability are directly applicable to evaluating plant accident sequences. In terms of the water ingestion mechanism, the test results indicate that the heat transfer correlation based on previous one-dimensional (SSWICS) tests is conservative insofar as calculating ingestion-limited crust growth behaviour. In particular, the correlation tends to under-predict the heat flux to overlying water during time intervals when water ingestion is occurring. In terms of the melt
eruption mechanism, significant eruptions have been observed in the case of limestone common sand concrete. Eruptions have also been observed under early cavity flooding conditions for siliceous concrete. For tests conducted in two dimensional cavity configurations, the eruptions appear to have occurred under a floating crust boundary condition, which is expected at plant scale. Finally, the crust breach data indicate that the crusts that form at the melt/water interface after cavity flooding have a low mechanical strength, and cannot be mechanically stable at plant scale. Rather, the crust is expected to fail and, thereby, maintain a floating crust boundary condition that will allow the melt eruption and water ingression cooling mechanisms to proceed.

It is noted that the debris coolability experimental database consists almost exclusively of tests conducted with oxidic melts. When a significant metal fraction is present in melt, it may result in a stratified pool configuration. This type of pool structure has not been evaluated from a coolability standpoint. Thus, additional analysis and testing may be required with melts containing a significant metal fraction to further reduce phenomenological uncertainties related to debris coolability, as well as core-concrete interaction.

**Capabilities and shortcomings in current codes and models**

One goal of this SOAR was to summarise and assess capabilities of various simulation tools currently used in the world, focusing on models used to describe the corium concrete interaction phenomena and the coolability mechanisms induced by top flooding. Chapter 3 of this report provides a generic description of MELCOR MCCI module (i.e. CORCON), CORQUENCH, COSACO, ASTEC MCCI module MEDICIS, TOLBIAC-ICB, WECHSL, COCO, MAAP MCCI module, and the SOCRAT MCCI module. More detailed models descriptions are also provided as in the appendices.

All codes can currently analyse the case in which the corium is assumed to be instantaneously spread over the entire floor of the reactor pit under dry cavity conditions. This situation is most consistent with rapid high temperature melt pour scenarios in dry cavities, for which efficient spreading can be expected. However, not all the codes can adequately model the impact of top quenching. In addition, the ability to analyse situations in which concrete ablation is limited to a part of the reactor pit walls and/or floor due to localised corium accumulations, as well as scenarios involving multiple pours separated in time, cannot be currently treated in an easy and systematic way using available codes. Moreover, many current codes are not able to treat MCCI scenarios that involve spreading into additional cavities adjacent to the reactor pit. Finally, the effect of concrete type on concrete erosion pattern viz., the experimental results for siliceous or limestone common sand concrete cannot be explained satisfactorily by existing phenomenological models.

Another aspect documented in Chapter 4 of the SOAR relates to validation and discussion of modelling. Detailed and often different models of various phenomena are recommended for use in different codes, as a result of individual code validation work that is often based on separate effect experiments. However, MCCI is a complex interaction of several phenomena and it is important to validate the codes against integral MCCI experiments to gain an appreciation of the predictive capability of these tools. The validation status of individual codes is described from a general viewpoint, with additional details regarding higher-level phenomenological models provided in the appendices. Since all models require material property data, the validation status of property data and supporting correlations are also reported, along with an assessment of how the experiment data can be scaled to reactor conditions based on available models in the codes.

The validation work is commonly focused on comparing key calculated results (i.e. corium temperature and local or maximum ablation depths) with the experimental data. Transient effects often impact the course of individual experiments – principally at the start of the interaction between the newly generated melt contacting cold test section structures, resulting in the formation of melt crust at the interface with the concrete that prevents its ablation – for which the codes cannot be assessed and
adequately validated since most codes do not have the necessary modelling capabilities. However, in the longer term, the experiments enter a quasi-steady regime in which code predictions of concrete ablation progression and temperature history are reasonably well understood. Good agreement with oxide melt experiments in terms of ablation and temperature history are noted when heat transfer coefficients on the order of 300 W/(m²K) and concrete decomposition temperature close to ~1,600 K are selected as boundary conditions at the melt-concrete interface.

Among other relevant findings from code assessment activities, it is noted that anisotropic ablation observed for siliceous concretes can currently be captured in the codes only via empirical application of heat transfer coefficients based on observations from two-dimensional experiments. The assessment of top flooding conditions on the course of the MCCI is not yet clear since the crust anchoring effect, observed in many experiments but not expected at reactor scale, is difficult to take into account and is modelled in very few codes. Uncertainties were identified for melts consisting of both oxides and metals. In particular, in a stratified pool configuration the metal melt thermal material properties suggest elevated heat transfer at the metal/concrete interface, but the overall transfer of decay power (which is predominantly released in the oxide melt) to the concrete via the metal layer is governed by the heat transfer at the interface between the oxide and the metal layer. Direct model validation for this interlayer heat transfer is not yet possible due to lack of appropriate data from experiments under MCCI conditions. Additional uncertainty is introduced due to a lack of knowledge regarding stratification and mixing processes under MCCI conditions. For situations in which concrete reinforcement serves as a continuous source of metal, its impact on the concrete ablation mechanism is still not properly understood.

Based on these underlying experiment and code assessments, an additional high level goal of this SOAR is to review applications of MCCI phenomena, models, and data to safety analysis of nuclear power plants under severe accident conditions, particularly in the context of reactor safety requirements and containment designs to address such requirements. Safety requirements, promulgated by various international bodies, are discussed as well as containment designs for a number of generation II and generation III plants with particular emphasis placed on features relevant to the MCCI issue. Three idealised plant (containment) configurations for reference plant calculations are also discussed, and a few example plant calculations are presented. These examples illustrate the approach of plant idealisation (simplification) that is quite common and reasonable in the field of safety analysis, noting the inherent uncertainties in severe accident phenomena. As with virtually all other severe accident phenomena, extrapolating the results of scaled experiments to MCCI in plant scale involves some idealisation of plant geometry and configuration. For full-scale plant safety assessment, the approach appears to be pragmatic whereby MCCI phenomena are analysed based on conservative assumptions with respect to the weaknesses of the containment design.

Remaining issues in the areas of MCCI and debris coolability

Notwithstanding the progress made in the field of core-concrete interaction, the apparent simplicity of the treatment of thermal-hydraulics of a well-mixed corium pool in the presence of the concrete decomposition gases is contrasted by the complexity of the concrete ablation mechanism where the heated concrete, a highly heterogeneous material, is gradually incorporated into the melt through an evolving melt-concrete interface that is still difficult to observe experimentally and capture from a modelling point of view. Because many of the models are not mechanistic, several parameters are often empirically fit to reproduce as best as possible the scaled experimental results. Attempts to model MCCI by a multi-scale computational approach to eliminate these tuned parameters with more mechanistic models have been unsuccessful to date. This is mostly due to the difficulty of observing and measuring local phenomena needed to validate multi-scale modelling approaches.
Based on the foregoing results, this SOAR focused on identifying remaining issues and residual uncertainties in the areas of MCCI and debris coolability, and formulates some recommendations for addressing these questions in the near future in order to increase the reliability of reactor simulations.

These remaining issues and recommendation can be classified into three general topics of interest that are: long-term core-concrete interaction behaviour; realistic plant simulations; and coolability enhancement under top flooding conditions.

**Long term core-concrete interaction behaviour**

The past MCCI experiments with prototypic materials were relatively short in duration, which is partially due to the fact that constraints did not allow significantly longer duration experiments. Admittedly, in many of these experiments the test duration was adequate to assure at least partial melt cooling and slowing down of basemat ablation to a level that could be considered acceptable for regulatory purposes (i.e. ablation limited to a specified amount by say 24 hours into the accident).

The Fukushima accidents suggest that a much longer transient is quite likely. A recent report on “Safety Research Opportunities Post-Fukushima – Initial Report of the Senior Expert Group” (NEA/CSNI/R(2016)19) noted that MCCI very likely occurred in one or more Fukushima Daiichi units for some time, but did not lead to a significant melt release outside the containment vessel to the reactor building. The analyses performed to date in the NEA Benchmark Study of the Accident at the Fukushima Daiichi Nuclear Power Station (BSAF) project phase 1 have not provided a consensus view on the MCCI issue. In particular, the termination of the MCCI process was found to have been impacted significantly by differences in melt pour conditions predicted by different codes at reactor vessel failure. These findings put into question those analyses results which predict ongoing MCCI for a long time, especially in the presence of water. Hence, there is a need to obtain longer duration experimental data if the shorter duration experimental data cannot be extrapolated to the reactor situation with a high degree of confidence.

**Extrapolation of existing knowledge to long term MCCI processes**

Longer duration experiments will provide data needed to: (1) confirm that intermittent phenomena like melt eruptions are reproducible; and (2) investigate if the crust formed by water ingression is stable. Long duration experiments will also provide data on long term behaviour dealing with the final phase of the interaction, i.e. the time when the heat flux to concrete is low enough that it can be dissipated by conduction into concrete without further ablation, or the heat flux that is applied in a specific coolant circuit of a core catcher. Finally, long term behaviour also refers to situations wherein the concrete fraction within the melt and the heat flux level are representative of the situation after many hours of interaction. The subject of long-term behaviour vis-à-vis further research needs and recommendations is discussed further in the following paragraphs.

It is noted elsewhere that more rapid radial ablation (relative to axial) was observed for siliceous concrete, whereas limestone common sand concrete showed an isotropic (uniform axial and radial) ablation profile. Currently, there is no generally accepted phenomenological explanation for this behaviour, and the question remains if it is reliable to extrapolate this result to reactor scale for a longer duration ablation process. This general scaling issue is equally important to concrete ablation in a wet cavity situation.

Another complex phenomena related to this issue is associated with intermittent ablation bursts that are observed in experiments. It is not clear if this is a result of crust instability or rather a result of concrete spalling due to mechanical instability. Depending on the phenomena, the characteristic time period can be several hours; e.g. crust dissolution processes with siliceous concrete. In this case the test duration has to be long enough to observe at least two or three ablation bursts. Data from these
long duration experiments will reduce uncertainties in current melt eruption models and will provide better confidence in extrapolating to reactor scale.

It is important to recognise that in every facility, the size of the test section, the heating technique, and/or the operating procedure always induces some transient system effects. These transient effects are not modelled in the codes and it is not required for most of plant applications. As a result when the transient effects are dominant, the codes cannot reproduce accurately the final cavity shape which is commonly used to estimate the ablation rate. It is recommended that an experiment objective should be to reduce the duration of the initial transient, and experiment techniques that can contribute to homogenous initial melting of the corium and limit initial crust formation should be encouraged. Furthermore, future tests should be designed to run under steady state conditions for a longer duration.

Under long test operating conditions involving top flooding, one systematic drawback of the experiments is the top crust anchoring phenomena. In tests performed with top flooding, the upper crust eventually anchored to the side walls. The anchoring phenomenon unrealistically reduces the efficiency of the melt ejection phenomena because a gap between the pool and the upper crust appears and then increases due to concrete densification upon melting as well as loss of liquid corium as eruptions occur. At the beginning of the process, crust anchoring could also create a pressure build-up effect below the crust that experimentally distorts the eruption process. Crust strength measurements made on crust samples obtained from experiments and supporting structural analyses indicate that a floating crust boundary condition is likely in full-scale reactor geometry. In this spirit, experiment techniques that can promote a floating crust boundary condition in reduced scale experiments should be encouraged.

The final step of the MCCI process is characterised by a core-concrete heat exchange surface so large that the heat can be transferred by conduction to the remaining concrete without further ablation. This scenario would yield a very viscous melt with high concrete fraction. Under such conditions, the heat transfer models at the core-concrete interface may not be valid. Some codes utilise a quasi-steady modelling approach in which conduction into the concrete is not modelled. Thus, all heat transfer from the core debris is dissipated by concrete ablation, and as a consequence, the ablation never stops. Some of these deficiencies in analytical tools can be addressed with data from longer duration experiments.

**Realistic plant simulations**

Improving the realism in plant simulations inherently introduces more complexity. As a result, the associated efforts have to be balanced with approaches that rely on invoking additional levels of conservatism to define a bounding set of hypotheses for safety-relevant issues. Three major related issues are: the presence of metal within the melt or within the concrete; the initial conditions for MCCI based on melt pour conditions into the reactor pit; and the presence of impurities in cooling water (e.g. seawater or brackish water).

**Presence of metal within the melt or within the concrete**

The presence of metal within the melt or within the concrete influences the ablation profile as soon as stratification occurs. The stratification process is governed by the higher density ratio between metal and oxides as soon as the fuel oxides become diluted with concrete oxides. While several experiments have been performed with iron-alumina thermite simulant, only a few tests have been performed with a fully prototypic metal-oxide core melt composition. It was not possible to establish from the results clear evidence of stratification, but ablation was observed to be increased in front of the metallic masses. For prototypic metal-oxide core melt, the oxidation kinetics and the stratification thresholds are important as they influence the time window when the melt is stratified at reactor scale and as a result, the prediction of the basemat melt-through time.
The initial phase of the melt-concrete interaction involving unoxidised cladding (zirconium) in the melt has been investigated in a few reactor material experiments. This stage can lead to highly exothermic metal oxidation reactions. Zirconium-bearing concrete-metal inserts were used in some Argonne experiments in which a relatively small amount of Zr was incorporated into the melt just prior to melt contact with the concrete basemat. However, it is likely that a significant fraction of the Zr in the inserts was oxidised before the test initiation, thereby limiting the impact on the actual MCCI phase of the experiment. Another aspect not investigated in experiments is the presence of uranium within the metallic phase. During the in-vessel stage of the accident, uranium is found in the metal phase in scenarios that lead to a significant fraction of unoxidised cladding in the lower head. Under these conditions it seems appropriate to implement an oxidation model for uranium in simulation tools and to perform sensitivity analyses.

Reinforcing bars in concrete play a double role as they are a continuous source of metal which is prone to oxidation during ablation and additionally, they change the ablation mechanism. In particular, some recent test results indicate that the presence of reinforcing bars in siliceous concrete leads to a homogeneous ablation profile, which contrasts the results from reactor material tests carried out with non-reinforced siliceous concrete in which anisotropic ablation was observed.

The presence of metallic inclusions in an otherwise oxidic crust could change the properties and thereby impact the water ingression cooling mechanism. Specifically, the presence of metal could influence the critical heat flux associated with cracks that form in the crust due to thermal contraction induced by top flooding. To address this issue, additional experiments could be performed with different metal contents in the melt and a representative gas release to promote good mixing conditions. For these tests, as well as large scale experiments with sustained heating, new thermite compositions need to be developed that would produce a melt with adequate metal fraction.

The effect of metallic inclusions in melt on the melt ejection cooling mechanism is different as it occurs over a longer duration. It would be interesting to evaluate the entrainment rate of pure metal melts and check the morphology of the particles formed during the quenching process to assess their coolability as well as their influence on the coolability of the particulate debris bed in general. As soon as the specific technological challenges of metal-oxide experiments are resolved, tests with high metal fraction are recommended.

Thermal stresses on concrete structures brought on by core debris interactions with concrete have not been investigated in MCCI Programs. These stresses are largely inconsequential for below grade reactor cavities but can be quite important for free standing cavities such as sub-atmospheric containments and especially for reactor pedestals in boiling water reactors. The core debris interactions place the inner region in compression where concrete is strong but the outer region in tension where concrete is weak and easily cracks. This has structural implications which again have not been investigated in MCCI Programs.

**MCCI initial conditions following melt relocation into the reactor pit**

It is often assumed that the MCCI phase starts as soon as the vessel fails and the corium mass in the lower head (which in bounding analyses includes the entire fuel and structural inventory in the reactor) is relocated into the reactor pit. This approach offers a degree of conservatism in terms of axial-melt-through delay if one assumes that the melt is spread instantly over the entire surface of a dry pit. However, when the reactor cavity is flooded, spreading may be limited leading to corium accumulation in one part of the reactor cavity. This scenario will result in higher heat fluxes to concrete and reduce the basemat melt through time if this accumulation is stable and does not eventually spread out uniformly. Among other things, local corium accumulation in the reactor cavity mainly depends on the corium temperature, corium pour rate, reactor pressure vessel (RPV) failure.
location, amount of water present in the reactor cavity at RPV failure, and finally on the ability to provide water continuously on top of the corium accumulation.

Such configurations are quite complex to study because they involve the formation and spreading of corium accumulations under water as well as the possibility of boiling off the water inventory, drying out the core debris, re-melting, and onset of concrete ablation. An ancillary issue is that core debris in a reactor cavity, if not covered by water, exposes a great deal of concrete surface area to intense convective and radiative heat flux. The gas generation and concrete degradation from this exposed concrete cannot be neglected in the analysis of core concrete interactions and containment integrity.

Depending upon the melt pour conditions and with a relatively shallow water layer, melt jet fragmentation is expected to be minor. For this type of scenario, existing MCCI models that treat the corium as an initially intact melt pool interacting with concrete may be employed as a reasonable approximation. However, for deeper water pools melt jet fragmentation may be significant, leading to formation of a coherent particle bed, or a compact melt layer commonly referred to as a cake surrounded by particle bed. Depending upon the bed depth, decay heat level, particle size, and porosity, the configuration may be coolable. However, if the dry-out limit for the bed is too low then gradual reheating, dry-out, melting, and onset of concrete ablation will occur.

These particular configurations have not been extensively investigated as part of MCCI research, nor can existing MCCI models address all of these configurations. However, there has been a significant amount of research done in this area (both experiments and modelling) that generically addresses particle debris bed coolability for both in-vessel and ex-vessel applications. Conducting experiments that involve dry-out and melting of particle beds composed of reactor materials is a technical challenge given limitations with current core debris heating techniques. Thus, a possible first step to address this issue is to utilise existing models to evaluate coolability of particle bed formations predicted for plant applications. If these analyses indicate that the beds are not likely to be coolable, then effort should be devoted to developing appropriate experiment techniques to address this type of behaviour.

Another related issue is that of multiple pours and how that affects the coolability of debris in the reactor pit. Again, in all experimental and analytical studies concerning MCCI, it is traditionally assumed that at RPV failure, the molten material (whether the entire reactor inventory or partial inventory) is poured all at once and instantly spreads on the whole reactor pit surface. It is likely that in some accident scenarios, the melt pour would be periodic which has two consequences: non-uniform melt accumulation and non-uniform spreading. Conducting experiments with this kind of melt configuration may be quite challenging, and an analytical extrapolation of experimental data for symmetric and uniform melt configuration may be more worthwhile based on simulant data.

**Presence of impurities in cooling water**

The impact of impurities in cooling water on severe accident behaviour resurfaced following the Fukushima accident. In particular, the use of sea water brought into question the impact of salt (sodium chloride) on coolability mechanisms, on fission products chemistry, and the performance (i.e., potential for clogging) of coolant loops. Generally speaking, any impurity in cooling water (whether it is salt in sea water or other forms of impurities in brackish inland fresh water) can impact one or more of these areas.

For the ex-vessel corium cooling mechanisms identified under top flooding conditions, the formation of precipitate in the cracks of the upper crust or in the overlying debris bed could reduce the dry-out limits for these formations. As the composition of water present in the sumps at the bottom of the reactor building is complex and may depend on the accident management strategy, it seems easier to address the issue in separate effect tests than in semi-integral experiments. Ongoing experiments in
Japan are addressing some aspects of the water impurity issue. To parametrically investigate the effect of water impurities on melt coolability by water ingestion, SSWICS-like tests could be run to evaluate the impact on the cracks formation and on the crust critical heat flux. If warranted, more complex experiments (i.e. with sustained heating) could be conducted to assess the behaviour over the long term. For the melt ejection mechanism, the influence of impurities on debris bed coolability could be investigated in separate effects tests that utilise existing facilities for investigation of dry-out in debris beds for in vessel conditions.

The water at the bottom of the containment building will be highly contaminated with fission products. If this water is used to cool the melt, the chemistry of the fission products will likely be modified by gas bubbling and more generally by particulate entrained in the water. While the fission product behaviour under such conditions is an ancillary issue related to the consequences of clogging, water samples could be collected quite easily at the end of MCCI experiments to perform chemical analysis in order to characterise the chemical composition. If some impurities in the water plays a role in trapping other species released during MCCI, it would be useful to carefully select the composition of the water before running these tests.

**Methods for improving melt coolability under top flooding conditions**

This SOAR is focused on ex-vessel corium coolability under top flooding which is largely regarded as a generic accident management strategy for ex-vessel melt stabilisation in existing plants. The improvement of melt coolability under top flooding conditions can also be viewed as a potential back-fitting strategy for operating reactors. Moreover, for new reactors, spreading and top flooding can be incorporated in the design phase as a generic approach.

In terms of improvement, it is noted that a larger initial corium spreading area will reduce the downward heat flux to the concrete and hence, reduce the likelihood of basemat melt-through. One approach for increasing melt spreading area for plants with limited floor space is to allow radial melt-through of a barrier with subsequent spreading of a portion of the melt into the reactor building. This situation is more likely for siliceous concrete but remains limited only to the level of corium above the breach elevation. This corium spreading strategy may be more effective in a dry cavity situation, one that also provides the benefit of eliminating the risk of steam explosion.

The coolability of debris can be more efficient if corium spreading is combined with flooding. Water ingestion mechanism is most efficient at the early flooding stage with little concrete present in the melt, and that the melt eruption mechanism is also most effective in the early phase of the corium-concrete interaction due to the higher melt gas sparging rate. Ideally, it is desirable to have an initially dry pit to maximise spreading and to avoid the risk of a steam explosion, followed by early flooding. In this case, the time window to add water is narrow and a subsequent melt pour after top flooding cannot be excluded.

Another consideration is the composition of the concrete that is used for the reactor basemat at the plant. For new plants, a recommendation can be made to consider high carbonate and/or hydrate contents for the concrete used for reactor basemat. For existing plants with a potentially too thin siliceous concrete basemat, a possible back-fitting measure could be to consider pouring an additional (sacrificial) layer of high carbonate and/or hydrate concrete. In this case, since the thickness of this additional layer can be limited for a specific plant site, the key piece of information needed is the efficiency of the melt ejection mechanism so as to ensure that the liquid melt is transformed into a coolable particle debris bed before reaching the original siliceous concrete.
Perspectives

In the coming years the examination of the debris in the three damaged Fukushima reactors will likely provide additional insights that will enhance the understanding of MCCI phenomena at large scale and under fully prototypic conditions. The findings will undoubtedly provide opportunities to gain additional confidence in the application of simulation tools to existing plants. They will also provide data and information for optimizing severe accident management strategies for existing as well as future plants.

One of the top level recommendations in the NEA-SAREF report (in preparation) is to organise an MCCI workshop to discuss current state of MCCI knowledge, identify knowledge gaps, and identify data needs to bridge the gaps – the idea being that the Fukushima decommissioning effort can be informed by the outcome of such a workshop while at the same time, data collected during the decommissioning activities can be optimised to bridge the MCCI knowledge gaps. In two companion studies (one on severe accident knowledge gaps post-Fukushima and the other on Fukushima forensic data needs), MCCI knowledge and data gaps were identified as high priority topics. These findings confirm that in order to perform experiments and additional analysis to address more realistic situations, it is necessary to improve the capabilities of existing facilities and to perform needed experiments to bridge the knowledge gaps and reduce residual uncertainties. Since experimental MCCI research with prototypic reactor materials is an expensive undertaking, a collaborative effort among various nuclear safety research organisations in different countries is highly recommended.
1. Introduction to the main phenomena involved: Brief description of accident phenomenology

Chapter 1 provides an overall picture of the whole content of this SOAR. Although most of the topics included in this section are thoroughly developed in further sections of this report, Chapter 1 provides the basic definitions and descriptions of relevant safety issues in order to get a better comprehension of every aspect of the severe accident issues addressed by this SOAR.

The outline of Chapter 1 is as follows. Section 1.1 provides two key definitions for this report: the concepts of melt stabilisation and severe accident termination.

Section 1.2 shows the main paths followed by a severe accident after the failure of the reactor pressure vessel (RPV) in a light water reactor, with a special focus on molten core concrete interactions and ex-vessel corium coolability mechanisms.

Section 1.3 provides the basic understanding of the concrete ablation by the corium and it also describes the corium coolability mechanisms by top flooding. Because of corium and concrete are the two major components involved in the severe accident phenomena addressed by this SOAR, it was deemed that a brief description of these two components should be provided, including an overview of their most relevant physical and chemical properties. Additionally, two safety significant phenomena are also discussed in this section: radial vs. axial concrete ablation and corium stratification.

Containment failure modes associated with molten core concrete interactions are described in section 1.4. Safety topics unaddressed by this SOAR are also included in this section.

Section 1.5 deals with general aspects of severe accident management and severe accident management strategies aimed at achieving the melt stabilisation. Both, Gen II and Gen III reactors are considered in this section. A brief description of recent back-fitting measures implemented or under consideration in some Gen II reactor is also included.

Finally, section 1.6 provides the safety prioritisation of the different phenomena addressed by this SOAR within the framework of severe accident phenomenology. This prioritisation is based on a number of expert judgement activities conducted by the European Union and the SARNET group.

1.1. The concept of melt stabilisation and severe accident termination

The terms “melt stabilisation” and “severe accident termination” are widely used along this report. Thus, a definition of these two key concepts is required, previously to any other technical information. The following definition for melt stabilisation and severe accident termination is taken from (Sehgal, 2006).

“A severe accident cannot be characterised as stabilised and terminated until core melt/debris has been cooled and quenched and kept in the latter stage for a long time. Achieving and maintaining coolability of the melt/debris is paramount, since fission product release and non-condensable-gas generation stops as the melt/debris temperature drops below ~ 1000 °C and containment integrity is not seriously challenged anymore, if containment cooling cycle has been established.”
1.2. Corium discharge from the RPV into the containment

Figure 1.2-1 gives the major paths of severe accident progression after a RPV failure for an existing light water reactor type with inverted containment atmosphere (Allelein & Burger, 2006). In order to focus on the relevant phenomena related to core-concrete interactions and corium coolability, hydrogen combustion processes within the containment and the potential containment failure by an in-vessel steam explosion (the alpha-mode failure) are excluded from Figure 1.2-1.

According to Figure 1.2-1, the first question deals with the RCS pressure at the moment of the RPV failure. If the RPV fails with high pressure in the RCS, the corium would be widely dispersed throughout the containment. These scenarios are considered in the Section 1.4.5.

Assuming that the RPV fails with low pressure in the RCS and that RPV does not fail catastrophically, e.g. due to steam explosion or by melt-through with subsequent unzipping/tilting of the whole lower head, but rather locally in a mode (“fish-mouth”-type failure), e.g. observed in FOREVER experiments (Theerthan, Giri, Karbojian, & Sehgal, 2002) and in low-pressure LHF experiments (Chu, Pilch, Bentz, & Behbahani, 1998), a substantial amount of the in-vessel corium inventory would pour onto the cavity floor. Section 1.3.1 provides more details about the total amount of corium discharged from the RPV and corium constituents.

Some cavities designs allow for the presence of water before the RPV failure. In addition, severe accident managements strategies allows for water injection into the reactor cavity before vessel breach. Under these circumstances, the following fuel-coolant interactions can occur (Park & al., 1992) (CSN, 1997).

- If an ex-vessel steam explosion occurs, the melt that is involved in the explosion process may be dispersed out of the cavity. However, the melt not directly involved in the explosion can still form a particulate debris bed. As mentioned in Section 1.4.7 debris bed coolability and ex-vessel steam explosion are beyond the scope of this report.
- If there is no energetic ex-vessel steam explosion, the melt jet can break up in the water pool by hydrodynamic forces. As a result, a portion of the initial melt will form a particle bed, whereas the remaining will form a cake. As it is shown in Section 1.4.7 melt jet break up and the associated debris bed coolability issues are beyond the scope of this work.

Figure 1.2-1 also shows the phenomenon termed steam spike. This phenomenon is considered in Section 1.5.2.

Under dry cavity conditions, the debris pours into the cavity and accumulates at its bottom. The debris transfers heat to the atmosphere by convection and radiation, and attacks the concrete substrate, possibly leading to the containment melt-through. Sections 1.3.3 and 1.4.1 provide more information for concrete ablation and basemat melt-through, respectively.
Recently, interest for another molten core concrete phenomenon has been renewed: the limited corium spreading in the reactor cavity. This phenomenon consists of an accumulation of the corium poured into the reactor cavity within a reduced part of the total floor area available for spreading. Limited corium spreading could occur both under wet and under dry cavity condition and could result in different concrete ablation profiles in comparison with the ones in total spreading case. Chapters 5 and 6 provide more detailed information about this recently considered phenomenon.

According to Figure 1.2-1, the branch without water in the cavity at the time of melt release considers the possibility of cavity flooding after vessel breach. Section 1.5.3 discusses the possibilities to reach a manageable situation when water is poured atop the corium located on the cavity floor.

**Figure 1.2-1: Exemplary paths of main severe accident phenomena in the cavity**
1.3. Corium concrete interaction

This section is aimed at providing a basic understanding on the concrete thermal attack by the corium and the ex-vessel corium cooling mechanisms. In order to get an appropriate understanding of the present research and analytical activities carried out in the field of core-concrete interactions and corium coolability, it is needed to obtain an appropriate background on the main characteristics, e.g. chemical composition, material properties and so on, of the two main substances involved in the topic addressed by this SOAR: corium and concrete.

The most relevant characteristics of the corium from the perspective of this report are described in Section 1.3.1. The amount of corium poured onto the cavity floor when the RPV fails and the corium constituents are also described in this section. It is well known how important is to obtain an appropriate knowledge of a number of material properties in the field of the severe accidents. Thus Section 1.3.1 also deals with the corium thermophysical properties within the field of core concrete interactions.

The main characteristics of the other relevant substance for this report, i.e. concrete, are described in Section 1.3.2. More specifically, concrete constituents, and chemical composition of the concretes used in LWR, some insights about the thermal response of the concrete under high heat fluxes and the thermophysical properties of concrete are the topics addressed in this section.

The concrete ablation phenomenon is described in Section 1.3.3. Because of their potential high impact on the plant safety two specific topics are also included in this section. The first one is concerned with the recent experimental evidence of the possible asymmetrical concrete erosion in some cases, i.e. some experiments have shown that the radial erosion rate could be faster than the axial erosion rate. The second topic included in this section deals with the impact in plant safety of the possible melt stratification during the MCCl process. More specifically, the influence on the timing of the basemat melt through of the stratified vs. homogeneous melt is briefly discussed.

Finally, Section 1.3.4 describes with some detail the main ex-vessel corium coolability mechanisms, namely: bulk cooling, water ingression, melt eruptions and crust breach.

1.3.1. Corium characteristics

Corium is a molten mixture of fuel material, partially or totally oxidised cladding, non-volatised fission products and various structural materials. The main constituents of in-vessel corium are UO$_2$, ZrO$_2$, Zr and steel.

Section 1.3.3.2 provides an example of a typical corium composition for a 900 MWe PWR. In this case, the total amount of corium discharged onto the cavity floor is $\approx 150$ tonnes. Oxidic corium amounts to $\approx 100$ tonnes (roughly, 67% of the total corium inventory). The remaining $\approx 50$ tonnes (one-third of the total corium inventory) forms the metallic phase of the corium, being Fe the main contributor to this corium phase: 35 tonnes, accounting for 70% of the total corium metallic mass. It should be kept in mind that the amount of molten steel varies greatly, depending on scenarios. As an example, it can exceed 100 tonnes (for up to 175 tonnes of oxides) in EPR, in which there is a heavy reflector (Nie, 2005).

In Three-Mile-Island 2 accident, a corium with the following average composition has been found in the vessel lower head (Akers & McCordell, 1989):

- 77 wt% UO$_2$, 17% ZrO$_2$, 2% Ag, 1% Fe, 1% Cr, 1% Ni, 1% In

Note that the oxidic phase accounts for the 94% of the total TMI-2 in-vessel corium mass.

As for BWR, the in-vessel corium is richer in metallic content than the corresponding one to PWR. (Greene, Hodge, Hyman, & Tobias, 1991) analyses the performance of BWR Mark III
containment during a short-term SBO. According to this analysis, the RPV fails at 3h38 min and the following data for the ex-vessel corium composition are provided in Table 1.3-1.

Table 1.3-1: Typical ex-vessel corium composition for BWR

<table>
<thead>
<tr>
<th>Constituent</th>
<th>Mass (tonnes)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Oxides</td>
<td></td>
</tr>
<tr>
<td>$\text{UO}_2$</td>
<td>163</td>
</tr>
<tr>
<td>$\text{ZrO}_2$</td>
<td>17</td>
</tr>
<tr>
<td>Others</td>
<td>0.2</td>
</tr>
<tr>
<td>TOTAL</td>
<td>180.2</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Metals</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Fe</td>
<td>121</td>
</tr>
<tr>
<td>Zr</td>
<td>48</td>
</tr>
<tr>
<td>Cr</td>
<td>23</td>
</tr>
<tr>
<td>Ni</td>
<td>10</td>
</tr>
<tr>
<td>$\text{B}_{12}\text{C}$</td>
<td>0.3</td>
</tr>
<tr>
<td>TOTAL</td>
<td>202.3</td>
</tr>
</tbody>
</table>

| TOTAL OXIDES + METALS | 382.5 |

(SNL, 2012) provides the following data for the debris bed composition prior to lower head failure for an unmitigated long-term SBO. Lower head failure occurs at 19.7 hours. Debris bed accumulated in the lower head of the RPV represents the mass of nearly the entire core plus structural materials below the core. This debris bed is composed of a mixture of molten stainless steel (~32% by mass), unoxidised zirconium (~12%) and particulate debris containing uranium dioxide and metallic oxides (the remainder). Failure of the lower head results in the rapid ejection of over 300 metric tonnes of core debris onto the floor of the reactor pedestal in the drywell.

Table 1.3.2 provides typical molar $\text{U/Zr}$ ratio for some LWRs.

Table 1.3-2: Molar $\text{U/Zr}$ ratio for some LWRs

<table>
<thead>
<tr>
<th>$\text{U/Zr}$</th>
<th>Reactor Type</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.65</td>
<td>US Boiling Water Reactor</td>
</tr>
<tr>
<td>1.64</td>
<td>US PWR (Farmer, Spencer, &amp; Aesclimann, 2000)</td>
</tr>
<tr>
<td>1.52</td>
<td>French 900 MWe PWR (Baichi, 2001)</td>
</tr>
<tr>
<td>1.37</td>
<td>TMI-2 PWR</td>
</tr>
<tr>
<td>1.24</td>
<td>EPR (Nie, 2005)</td>
</tr>
</tbody>
</table>

1.3.1.1. Corium chemical thermodynamics

The list of physical and thermodynamic properties which plays a role in corium progression during a severe accident is quite impressive: liquidus and solidus temperatures, enthalpy, density, viscosity, thermal conductivity, emissivity, surface tension and so on. Additionally, most of these properties vary significantly with the composition and/or the temperature of the mixture. As a result, a prioritisation should be made according to their importance for the safety in order to conduct research activities aimed at obtaining appropriate data for these variables. As far as the ex-vessel corium behaviour and
cooling is concerned, the knowledge of phase diagrams and viscosities are generally considered as the most relevant variables.

Chapters 3 and 4 of this report provide detailed information about the modelling of relevant material thermophysical and thermodynamic properties within the field of corium concrete interactions.

Dedicated thermodynamic databases (Fukasawa, Tamura, & Hasebe, 2005), (Guéneau, et al., 2006), (Cheynet, NUCLEA, 2007), (Bakardjieva, et al., 2010) have been developed and are coupled with Gibbs Energy minimisers in order to estimate the phases present in equilibrium for any corium composition and temperature.

Now, data about some relevant material properties are provided. UO$_2$ melting point is at $3120\pm 30$ K (Fink & Petri, 1997). This value can only be applied to non-irradiated fuel. Irradiation tends to increase the oxygen stoichiometry to UO$_{2+x}$ and decrease by at most a few hundreds of K the liquidus temperature. The minimum liquidus temperature in the UO$_2$-ZrO$_2$ system is of the order of $2840$ K (Cohen & Schaner, 1963). When concrete is mixed with corium, the solidus temperature is lowered to about $1400$ K, as shown in Figure 1.3-1. The solidification range spans over more than $1000$ K for most of corium-concrete mixtures, which facilitates corium spreading.

![Figure 1.3-1: Typical corium–concrete pseudo-binary phase diagram calculated with GEMINI2 and NUCLEA07 (for the silica-rich concrete used in VULCANO) (Journeau & Piluso, 2012)](image)

1.3.2. **Concrete characteristics**

Concrete is a complex mixture of mainly cement, water, aggregates and additives (in a small amount). Cement, usually in powder form, acts as a binding agent when mixed with water and aggregates. Water is needed to chemically react with the cement (hydration) and to provide
workability to the concrete. Typical concrete is made out of two different types of aggregate substances: a coarse aggregate (usually gravel) and a fine aggregate or sand. Sand\(^1\) or fine aggregate is generally defined as aggregate with a size lower than 4.75 mm. Coarse aggregate are above 2 mm and seldom beyond 25-40 mm, therefore there is an overlap. Although aggregates are generally thought of as inert filler within a concrete mix, a closer look reveals the major role and influence that aggregate plays in the properties of both fresh and hardened concrete. Changes in gradation, maximum size, unit weight, and moisture content can all alter the character and performance of the concrete mix.

Chemical additives can improve concrete performance by modifying its characteristics and enhancing workability. Chemical additives have to be added during the concrete manufacturing process, under the appropriate conditions, at the convenient speciation and at the exact quantities. Air-entraining agents are one example of chemical additives. Air entrainment is the process whereby many small air bubbles are incorporated into concrete and become part of the matrix that binds the aggregate together in the hardened concrete. Entrained air must be used in all concrete that will be exposed to freezing and thawing and deicing chemicals. Additionally, air-entrained concrete is more workable than non-entrained concrete. As other examples, the addition of gypsum\(^2\) retards the curing and limits the heat release associated with curing concrete in large placements such as reactor basement foundations or the addition of polypropylene fibres enhances concrete porosity during a thermal attack in order to prevent concrete spalling.

Concrete is usually reinforced by steel rebars. Depending on the design, steel rebars can account for 6-16% in mass of the structural concrete. Steel rebars are a supplementary source of metal in the long-term molten core concrete interaction (Journeau & Piluso, 2012). As it is shown in Chapter 6, MOCKA experiments are providing additional insights on the effect of the steel rebars on the concrete ablation profile (Foit, Fischer, Journeau, & Langrock, 2014), (Foit J. J., 2015).

Table 1.3-3 summarises the proportion of the different constituents of concrete (Journeau & Piluso, 2012).

**Table 1.3-3: Order of magnitudes of concrete constituent proportions**

<table>
<thead>
<tr>
<th>Constituents</th>
<th>Water</th>
<th>Air</th>
<th>Cement</th>
<th>Aggregates</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Vol %</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>14 – 22</td>
<td>1 – 6</td>
<td>60 – 78</td>
<td>25 – 45</td>
</tr>
<tr>
<td>Evaporable (a)</td>
<td>-</td>
<td>7 – 14</td>
<td>25 – 45</td>
<td>25 – 45</td>
</tr>
<tr>
<td>Chemically bond (b)</td>
<td>-</td>
<td>14</td>
<td>18</td>
<td>25 – 45</td>
</tr>
<tr>
<td>Wt %</td>
<td>5 – 9</td>
<td>1.5 – 4</td>
<td>65 – 85</td>
<td>30 – 45</td>
</tr>
<tr>
<td>2.5 – 7</td>
<td>0</td>
<td>30 – 45</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

- Evaporable water consists of free and physically bound (absorbed) water. It is defined as the water released after drying in a furnace at 105 °C.
- Chemically bound water is forming hydrates which are stable at 105 °C, mainly tobermorite gel \(\sim (\text{CaO})_{1.62}\text{SiO}_2\text{H}_2\text{O})_{1.5}\) and calcium hydroxide \(\text{Ca(OH)}_2\).

In fact the water proportion can vary as it depends also on the operating temperature and level of humidity.

1. Sand is usually associated with silica but in some regions fine aggregates are produced only from crushed limestone.
2. The potential of sulfur release from the gypsum is of considerable interest for both source term considerations and for the performance of passive autocatalytic hydrogen recombiners whose catalysts are irreversibly poisoned by sulfur.
More detailed information about the chemical composition of typical concretes used in LWR is shown in Table 1.3.4. Note that concrete for LWR is mainly comprised of silica (SiO2) and limestone (CaCO3).

In addition to above-listed typical concretes used for the civil structure in LWR plants, other types may be applied for specific purposes, like in Russian designs to shield the radiation in the lower cavity. Such concretes are typically fabricated with serpentine aggregates. Serpentine is a mineral of the composition (MgO)6(SiO2)4(H2O)4 with a substantial content of water in a crystalline structure. Another specific application of concrete, is its use as sacrificial material for severe accident mitigation, as in the EPR™ reactor (Nie, 2005), where, the aggregates used for the concrete in the reactor pit contain a large fraction of hematite (Fe2O3-rich ore).

Table 1.3-4: Chemical compositions3 of typical concretes used in LWR (values in w %)

<table>
<thead>
<tr>
<th>Specie</th>
<th>Siliceous (Roche, Leibowitz, &amp; Fink, 1993)</th>
<th>Basaltic (Roche, Leibowitz, &amp; Fink, 1993)</th>
<th>LCS (Roche, Leibowitz, &amp; Fink, 1993)</th>
</tr>
</thead>
<tbody>
<tr>
<td>SiO2</td>
<td>68.99</td>
<td>52.80</td>
<td>28.27</td>
</tr>
<tr>
<td>CaO</td>
<td>13.47</td>
<td>12.51</td>
<td>26.02</td>
</tr>
<tr>
<td>Fe2O3</td>
<td>1.00</td>
<td>8.58</td>
<td>1.64</td>
</tr>
<tr>
<td>MgO</td>
<td>0.7</td>
<td>3.03</td>
<td>9.62</td>
</tr>
<tr>
<td>Na2O</td>
<td>0.69</td>
<td>3.06</td>
<td>1.09</td>
</tr>
<tr>
<td>Specie</td>
<td>Siliceous (Roche, Leibowitz, &amp; Fink, 1993)</td>
<td>Basaltic (Roche, Leibowitz, &amp; Fink, 1993)</td>
<td>LCS (Roche, Leibowitz, &amp; Fink, 1993)</td>
</tr>
<tr>
<td>K2O</td>
<td>1.41</td>
<td>1.41</td>
<td>0.57</td>
</tr>
<tr>
<td>Al2O3</td>
<td>4.4</td>
<td>11.26</td>
<td>3.48</td>
</tr>
<tr>
<td>CO2</td>
<td>4.23</td>
<td>-</td>
<td>21.41</td>
</tr>
<tr>
<td>H2O (ev</td>
<td>3.68</td>
<td>4.23</td>
<td>6.13</td>
</tr>
<tr>
<td>chem.)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Depending on the region where aggregates are mined the composition of calcareous aggregates can vary. If calcareous aggregates come often from calcite limestone it is possible to find also dolomite MgCa(CO3)2 which decomposes at lower temperatures. Siliceous aggregates can span also a large range of compositions and properties. Basaltic aggregate is, for example, a eutectic material that is relatively fluid when melted whereas higher silica content aggregates like granite can be higher melting and much less fluid when they melt.

The response of concrete to high heat fluxes is complex. Concrete is an inhomogeneous material which undergoes changes in composition as it is heated. Several authors have described the reactions that occur with an increase of temperature in concrete. Below, the most relevant chemical processes during the concrete heat-up are shown (Journeau & Piluso, 2012).

- 30–105 °C. The evaporable water and part of the bound water escape. It is generally considered that the evaporable water is completely eliminated at 120 °C.
- 180–300 °C. The loss of bound water from the first stage of decomposition of the CaO-SiO2-\(\text{H}_2\text{O}\) and carboaluminate hydrates takes place.

3. Figures may vary according to the exact composition of sands, aggregates and cements.
- From 300 ºC. Increase in porosity and microcracking.
- 374 ºC. Critical point of water above which no free water is possible.
- The EUROCODE (AFNOR, 2005) proposes to consider for simplified calculations that concrete above 500 ºC has no mechanical strength.
- 573 ºC. α → β transformation of quartz aggregates.
- 700 – 900 ºC. Decarbonation of calcium carbonate in cement paste and carbonate aggregates which forms CaO powder.
- 720 ºC. Second stage of decomposition of CaO -SiO2-H2O.
- From 1 100 to 1 250 ºC. Start of concrete melting. More details about the concrete melting temperature are provided below.

Concrete heating process also changes its thermo-physical properties, mainly due to drying and decomposition (Journeau & Piluso, 2012). Concrete density is generally around 2 000 – 2 500 kg m⁻³. The specific heat of concrete typically lies between 900 J kg⁻¹ K⁻¹ (at room temperature) and 1 100 J kg⁻¹ K⁻¹ above 400º C. These are averaged values that may be higher in certain temperature intervals, e.g. when chemical decomposition or evaporation processes take place. The thermal conductivity of concrete is generally low (a few W/(m.K)) and significantly decreases with temperature. (Roche, Leibowitz, & Fink, 1993) have measured the solidus and liquidus temperatures for several types of concretes. Table 1.3-5 shows the results.

<table>
<thead>
<tr>
<th>Solidus temperature (ºC)</th>
<th>Siliceous</th>
<th>Lime-siliceous</th>
<th>Calcareous</th>
</tr>
</thead>
<tbody>
<tr>
<td>Solidus temperature (ºC)</td>
<td>1 130</td>
<td>1 120</td>
<td>1 220</td>
</tr>
<tr>
<td>Liquidus temperature (ºC)</td>
<td>1 250</td>
<td>1 295</td>
<td>2 305</td>
</tr>
</tbody>
</table>

As it was previously mentioned, concrete starts melting around 1 100 to 1 250ºC. If the formation of a liquid concrete layer is considered as the reference transition, then the effective melting temperature generally corresponds to the temperature at which 30 – 50 vol% is liquid.

The decomposition enthalpy to bring concrete from room temperature to total melting is generally between 1.8 MJ kg⁻¹ to 2.5 MJ kg⁻¹. To provide an order of magnitude, it requires 2 MJ kg⁻¹ to heat a typical siliceous concrete from room to melting temperature, while 2.5 MJ kg⁻¹ are needed for a limestone-rich concrete. This higher value is both due to the decarbonation latent heat and the higher effective melting temperature.

When applying corium concrete heat transfer models based on a global heat transfer coefficient, the relevant temperature at which the concrete ablates, the so-called ablation temperature, needs to be specified. Unfortunately, the ablation temperature is not a thermodynamic quantity, at least not a known thermodynamic quantity (Epstein, 1997). In the Sandia MCCI test programme the concrete ablation front was identified with the 1 600 K isotherm as determined by the thermocouples cast into the concrete basemat. In the Argonne ACE test programme a thermocouple reading of 1 673 K was assumed to indicate the passing of the concrete ablation front. For the theoretical calculations shown in (Epstein, 1997), an ablation temperature of 1 600 K was chosen. As a general procedure that can be applied for various concretes (Nie, 2005) proposed to use the temperature at which the volumetric liquid fraction in the concrete slag has reached 50%. Correspondingly, the concrete decomposition enthalpy would be given by the heat required to lift the concrete from room to the thus-calculated
ablation temperature. Due to the observation that silica gravel was not melted at ablation (e.g. (Journeau, et al., 2012), it has also been proposed to consider ablation temperature as the temperature at which a significant liquid fraction has been reached for the mortar (cement and sand).

It also should be noted that the heat required to ablate concrete is relatively insensitive to the chosen temperature selected as the ablation temperature in (Epstein, 1997).

1.3.3. Concrete ablation

The attack of concrete by core debris is largely thermal in a LWR (Bradley, Gardner, Brockmann, & Griffith, 1993). Energy is generated in the core debris from radionuclide decay and from chemical reactions and may be lost either its top surface or to the adjacent concrete. In either case, as long as the heat source is sufficiently large, the situation rapidly approaches a quasi-stationary state where the losses from the core debris balance the internal sources.

It has been experimentally shown that thermal ablation was the major process governing core-concrete interaction. Molten core concrete interactions have a highly complex phenomenology coupling concrete high-temperature behaviour, molten pool thermal hydraulics, thermochemistry, mechanics, etc. (Journeau & Piluso, 2012).

The corium typically contacts concrete at an initial temperature higher than 2 400°C. The first instant of core-concrete interactions are controlled by the melt overheat which heats the concrete, leading to the liberation of steam and carbon dioxide and to the initial melting of concrete. Concrete decomposition bubbles will enter the corium pool, agitate it and, when available, oxidise the metal components, basically in the sequence Zr, Cr, Fe (Powers, Brockmann, & Shiver, 1986). Oxidation of Zr and Cr is a highly exothermic process that contributes in the initial interaction phase to significant energy release in the melt. Heats of reaction for Zr oxidation are:

$$\text{Zr} + 2\text{H}_2\text{O} \rightarrow \text{ZrO}_2 + 2\text{H}_2 + 600 \text{ kJ mol}^{-1} \text{Zr}$$

estimated at 2,500K with NUCLEA091 thermodynamic database and GEMINI2 from THERMODATA (Cheynet, 2007).

$$\text{Zr} + 2\text{CO}_2 \rightarrow \text{ZrO}_2 + 2\text{CO} + 600 \text{ kJ mol}^{-1} \text{Zr}$$

In addition to the reactions of metals with gases from the concrete, the so-called condensed phase reactions between oxides and metals should be also considered. These condensed phase reactions were highlighted by the SURC-4 experiment (Bradley, Gardner, Brockmann, & Griffith, 1993), (Copus, Blose, Brockmann, Gopmex, & Lucero, Core-Concrete Interactions using Molten Steel with Zirconium on a basaltic Basemat: The SURC-4 Experiment, 1989). SURC-4 showed a vigorous interaction between metallic zirconium and a siliceous concrete with low gas content. In order to explain this interaction, condensed phase chemical reactions between the Zr and concrete molten oxides should be taken into account. The driving chemical reaction was:

$$\text{Zr} + \text{SiO}_2 \rightarrow \text{Si} + \text{ZrO}_2 + 2.1 \text{ MJ/kg Zr}$$

At high temperature, this reaction can be accompanied by an incomplete oxidation reaction leading to the formation of SiO which leaved the pools surface as gas.

Condensed phase reactions are particularly important for core debris interactions with higher silica, lower gas concretes such as the one used in the SURC-4 experiment. They are less important for calcareous concrete that have low silica content and high gas content. Chapter 2 provides more details about the SURC-4 experiment.

After this initial transient, lasting from 10 min to a few hours depending on reactors and scenarios, the main source of heat will be the decay heat, which is mainly (>90%) generated in the oxide phase.
The so-called spalling phenomenon, e.g. the breaking off of the layers or pieces of concrete from a concrete surface exposed to high heat fluxes, plays a minor role in MCCI (Journeau & Piluso, 2012), (Sevón, et al., 2010).

The ablation rate is, at the first order, the ratio of the heat flux to the enthalpy needed to heat and melt a unit volume of concrete:

\[ v = \frac{\phi}{\rho \left( H_{\text{concrete melting}} - H_0 \right)} \]  

**Equation 1.3-1**

Where \( v \) is the ablation rate, \( h \) is the heat flux, \( \rho \) the solid concrete density, \( H_{\text{concrete melting}} \) the enthalpy at the “concrete melting” temperature (see Section 1.3.2) and \( H_0 \) is the concrete enthalpy at room temperature. Note that Equation 1.3-1 highlights that core-concrete interaction is characterised by an imposed power, and not by an imposed temperature (Sehgal, 2011). This approximate expression for the concrete ablation rate is valid as conduction in the still solid concrete is so low that the ablation front progresses as least as quickly as the conduction heat wave, thus, conductive heat losses are negligible at the first order. Some rough data about concrete ablation rates are provided below (Journeau & Piluso, 2012), (Sehgal, 2011). For a typical PWR of 1300 MWe, about 120 t of oxides (UO\(_2\), ZrO\(_2\), …) and 80 t of metal (Fe, Cr, Ni, Zr) could relocate into the reactor cavity. For a 6 m diameter cavity, this leads to a melt height of the order of 1 meter. For a 1-m thick corium pool and an initial decay heat of 15 MW, considering an isotropic heat flux distribution of 200 kWm\(^{-2}\), a simple calculation gives an average initial ablation rate of the order of 20 cm/hr. Typical long term ablation rates are several centimetres per hour.

The concrete decomposition products are miscible with corium oxides, so, during its interaction with concrete, the melt will be continuously enriched in concrete decomposition products (mainly CaO and SiO\(_2\)). If several metres of basemat are eroded, silica and/or calcia can become the major constituents of the melt, as it occurred during Chernobyl accident (due to mixing of corium with biological protection sand), with a typical melt composition of the so-called “brown-chocolate” (Pazukhin, 1994) corium, in which concrete and mineral protective materials have been mixed:

- 57% wt% SiO\(_2\)
- 11% Al\(_2\)O\(_3\)
- 8% UO\(_2\)
- 6% ZrO\(_2\)
- 6% CaO
- 6% MgO
- 5% Na\(_2\)O
- 1% Fe\(_2\)O\(_3\)

It is thus clear that there is not a unique corium-concrete mixture composition but a continuum of possible compositions depending on the reactor, the accident scenario and the instant in this scenario. Therefore, it is not possible to measure the corium-concrete mixture properties for every case and models have to be derived in order to estimate both its thermodynamic and thermophysical properties.

Chapters 3 and 4 provide detailed information about modelling of material thermophysical and thermodynamic properties.

The heat transfer mechanisms are strongly dependent on the (varying) compositions of the melt and concrete. The heat flux through a given interface is given by

\[ \phi_i = h_i (T_{\text{pool}} - T_i) \] 

**Equation 1.3-2**

where \( h_i \) is the convective heat transfer coefficient (mixed convection due to the sparging gases) and \( T_i \) is the interface temperature for the considered boundary. A number of heat transfer coefficient correlations have been developed for the heat transfer to the pool walls as well as for heat transfer between two immiscible liquid layers. More detailed information about these heat transfer coefficient correlations can be found in Chapters 3 and 4.
1.3.3.1. **Radial vs axial ablation**

Recent experiments on MCCCI addressed mainly two subjects, namely the 2D aspects of the ablation, e.g. axial versus radial erosion, and the role of crust formation and melt segregation upon onset of solidification. These subjects are especially important in the analysis of the long-term erosion and cavity formation. This section discusses the topic of radial vs. axial concrete erosion.

One of the major findings of these tests with pure oxidic melts on 2D erosion is the observation that dry ablation tests with silica-rich concretes tend to present at least after some transient phase an anisotropic ablation pattern, namely, radial rate ablation is faster than the axial ablation rate, resulting in a more efficient ablation of the sidewalls compared to downwards ablation. This is the major result of the CCI and VULCANO VB-U tests series. The synthetic views of ablation profile along 11 section axes of Chernobyl basaltic basemat, resulting from ISTC CHESS R&D shows that in most of the cases, the same preferential lateral erosion is observed (factor : ~2-4) (Pazukhin, 1994).

At the opposite, the tests with limestone-rich concrete (e.g. CCI-2, CCI-4) showed a more isotropic ablation.

Chapter 2 provides detailed descriptions for these experiments. Chapter 6 provides insights about the extrapolation of these experimental results to reactor scale. As for Gen. II reactors, safety implications of a potential radial melt-through and the subsequent corium spreading in rooms adjacent to the reactor cavity are addressed in Chapter 5.

1.3.3.2. **Homogeneous vs stratified pool melt**

According to (Bradley, Gardner, Brockmann, & Griffith, 1993), experimental evidence shows that the various oxidic species in the melt are highly miscible. The same holds true for the metallic species. In absence of bubbling flow, it should be expected that the core debris will stratify into distinct layers based on their relative densities.

The stratification of metal and oxide melts under gas bubbling is a complex phenomenon and it remains a real challenge (Sehgal, 2011). This phenomenon is influenced by the density differences, by the viscosities, by the size distribution of metal droplets and by the convection processes in the melt induced by the gas released from concrete.

In order to illustrate the safety relevance of the corium melt stratification a number of ASTEC calculations are described below (Sehgal, 2011). Table 1.3-6 shows the main data for the reactor geometry and the initial conditions and corium composition considered in the ASTEC calculations.

**Table 1.3-6:** Initial Conditions, reactor Pit Geometry and evolution of the decay power

<table>
<thead>
<tr>
<th>Initial corium inventory (tonnes)</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>UO₂</td>
<td>82</td>
</tr>
<tr>
<td>ZrO₂</td>
<td>19.5</td>
</tr>
<tr>
<td>Fe</td>
<td>35</td>
</tr>
<tr>
<td>Cr</td>
<td>6</td>
</tr>
<tr>
<td>Zr</td>
<td>4.8</td>
</tr>
<tr>
<td>Ni</td>
<td>4</td>
</tr>
<tr>
<td>Reactor cavity radius</td>
<td>3 m</td>
</tr>
<tr>
<td>Concrete characteristics</td>
<td>siliceous concrete with 6% Fe.</td>
</tr>
<tr>
<td>Basemat thickness</td>
<td>3 m to 4 m</td>
</tr>
<tr>
<td>Initial corium temperature</td>
<td>2,673 K</td>
</tr>
</tbody>
</table>
Note, that no corium quenching by flooding was taken into account in the present calculations.

Because of the present large uncertainties on the pool configuration and its possible evolution, three different scenarios concerning the pool configuration were considered in the ASTEC calculations.

- **Scenario 1.** This scenario assumes that a homogeneous oxide-dominated melt configuration is maintained during the whole MCCI phase. This configuration leads to rather slow concrete erosion.
  - Basemat melt-through occurs around 9 days for a basemat thickness of 4m.

- **Scenario 2.** It is assumed in this unlikely scenario that a fixed stratified melt configuration is maintained throughout the calculations what is a very conservative assumption. This configuration leads to the fastest concrete erosion.
  - Timing for basemat melt-through ranges between 14 hours only and 3.4 days, depending on the choice of the solidification temperature and on the basemat thickness.

- **Scenario 3.** Finally, a more realistic scenario is assumed with four successive phases. In this scenario, the pool configuration is initially stratified with the oxide layer below the metal layer because of the higher initial oxide density, followed rapidly by a homogenous pool. Then, the pool is again stratified but with the metal layer below the oxidic one, and finally the pool becomes again homogeneous after disappearance of the metal layer due to oxidation. This evolution of the pool was evaluated using pool configuration switch criteria consistent with BALISE experiments. The consequence of modelling this sequence of pool configurations is the large delay of the predicted melt-through time by more than 24 hours, compared to that obtained in the case of a steady stratified metal/oxide configuration.
  - Scenario 3 results are deemed as very conservative because pessimistic boundary conditions were chosen and the BALISE stratification criterion is very conservative.

Similar scenarios were run assuming LCS concrete. Longer melt through times were obtained for LCS concrete due to two peculiarities of the type of concrete. On the one hand, LCS concrete poses higher gas content than siliceous concrete (see Table 1.3.4). Higher concrete gas content leads to higher superficial gas velocities that, in turns, result in reducing the time duration of the stable stratified configuration phase or even suppressing it. On the other hand, ablation enthalpy for LCS is higher than the one for the siliceous concrete due to the high decomposition enthalpy of carbonates, causing a slower ablation velocity (see Section 1.3.2).

It is important to keep in mind that these layered configurations represent an idealised modelling assumption: so far these layered configurations have never been observed experimentally under prototypical conditions. However, usually both stratified and mixed configurations are considered in the analyses.
After the end of the MCCI, in the absence of gas induced mixing and heating, the oxide and metal layer should stratify according to their representative densities. A corresponding behaviour was observed in the BETA, LACOMERA, MOCKA and SICOPS experiments. Deviating from this, the recent VULCANO VB-US tests showed vertical regions filled with steel tongue as well as large floating metallic drops. This phenomenon may either be explained by peculiarities of the gas flow (and probably not of the heating) or by the presence of convective plumes observed within the melt (Journeau, et al., 2012).

1.3.4. Ex-vessel corium coolability mechanisms

Cooling of ex-vessel core debris using an overlying water pool was previously investigated in the Melt Attack and Coolability Experiments (MACE) Program at Argonne National Laboratory (ANL), under the sponsorship of EPRI (see Section 2.4.5). Large-scale integral experiments (with a test section lateral span up to 1.20 m) were conducted with melt masses of reactor prototypic materials ranging up to 2 metric tonnes. These experiments suggested various heat transfer mechanisms that could provide long-term debris cooling. More recently, ANL launched the NEA-MCCI Program (under the auspices of the NEA) to address two specific issues. The first issue relates to several debris cooling mechanisms identified in the MACE Program (specifically, the relative roles of these mechanisms to achieve overall coolability), while the second issue relates to long-term, two-dimensional concrete ablation by ex-vessel core debris under both dry and flooded cavity conditions.

Several heat transfer mechanisms have been identified through experiments that can contribute to long-term corium coolability. These mechanisms are summarised in Table 1.3-7, while a physical illustration is provided in Figure 1.3-2. In general, when water is introduced atop an MCCI, the question of whether or not a significant amount of the thermal energy is initially removed will depend upon whether a stable crust is able to form that inhibits heat transfer from the melt to the water over layer. For a stable crust to form, two necessary conditions must be met: i) a thermal condition, viz., the melt/water interfacial temperature must fall below the corium freezing temperature, and ii) a mechanical condition, viz., the incipient crust must be stable with respect to local mechanical loads imposed by the agitated melt. If either of these two conditions is violated, then stable crust formation is precluded. In this bulk cooling regime, efficient melt/water heat transfer occurs due to conduction and, predominately, radiation heat transfer across the agitated (i.e. area enhanced) melt/water interface, in addition to entrainment of melt droplets into the water over layer.

(a) (b)

Figure 1.3-2: Traditional view of (a) CCI with conduction-limited upper crust at melt water interface; (b) CCI with water ingression and melt eruption cooling mechanisms
As bulk cooling heat transfer continues, the melt temperature gradually declines. As the downward heat transfer rate decreases, then melt sparging arising from concrete decomposition also decreases. Thus, a point will eventually be reached at which the thermal and mechanical thresholds for interfacial crust formation are both satisfied, and an insulating crust forms between the coherent melt zone and water layer. The physical configuration at this point consists of an ongoing MCCI at reduced temperature with a crust atop the melt (Figure 1.3-2(a)). The crust will be characterised by some degree of porosity, or cracks, owing to the necessity of venting concrete decomposition gases.

After incipient crust formation, completion of the quench process can only be achieved if one of two conditions is met. The first condition is that the melt depth lies below the minimum depth at which decay heat can be removed via conduction heat transfer alone (~10 cm). This case is trivial, and is not addressed in any further detail. The second condition is that water is able to penetrate into the debris by some mechanism to provide sufficient augmentation to the otherwise conduction-limited heat transfer process to remove the decay heat. Three potential mechanisms have been identified through experiments which provide pathways for water to penetrate the debris. The first mechanism is water ingress through interconnected porosity or cracks (Figure 1.3-2(b)). This process relies on crack propagation through the material and, as such, is highly dependent upon the mechanical properties, since thermal stress is a key factor.

**Table 1.3-7: Summary of coolability mechanisms observed in MACE integral tests**

<table>
<thead>
<tr>
<th>Mechanism</th>
<th>Description</th>
<th>Experimental Evidence</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bulk Cooling</td>
<td>Melt sparging rate is initially high enough to preclude stable crust formation at melt/water interface. As a result, high heat transfer rates occur due to conduction and, predominately, radiation across the agitated (area enhanced) interface. This phase is terminated when a stable interfacial crust forms.</td>
<td>High heat transfer rates measured during early phase of the melt-water interaction. Data indicates that a coherent crust cannot form; rather, crust segments are broken up and mixed into melt.</td>
</tr>
<tr>
<td>Melt Eruptions</td>
<td>Melt dispersal occurs by an entrainment mechanism in which concrete decomposition gases carry melt through defects in the crust to the overlying coolant. The dispersed material is quenched and forms coolable particle beds and high surface area volcanic formations.</td>
<td>Eruptions observed in all tests conducted with limestone-common sand concrete after incipient crust formation and in test with siliceous concrete with early flooding (CCI6). The particle beds are characterised by high porosity and large particle size.</td>
</tr>
<tr>
<td>Water Ingression</td>
<td>Corium shrinkage from an initially molten to a fully quenched state amounts to ~ 18 vol%. This causes voids/defects to appear in the frozen material. Water penetrates down through the voids/defects, augmenting the otherwise conduction-limited heat transfer process.</td>
<td>Melt/water heat flux far exceeds that which could be transferred by conduction across the (up to 10 cm) thick crusts formed during the tests. Post-test measurements indicate that crusts are permeable to both gas and water flows.</td>
</tr>
<tr>
<td>Crust Breach</td>
<td>Due to water ingestion, thick crusts will form and bond to the reactor cavity walls. These crusts will not be stable in the typical ~ 6 m span of most plants. Thus, they will periodically fail, leading to renewed cooling by the above three mechanisms.</td>
<td>Partial crust failure and relocation events observed in MACE integral effects tests. Various structural – mechanical analyses have shown that crusts will not be stable at reactor scale.</td>
</tr>
</tbody>
</table>

The second mechanism is particle bed formation through “volcanic” eruptions. In this case, concrete decomposition gases entrain melt droplets into the overlying coolant as they pass through the crust. The entrained droplets then solidify in the overlying coolant and accumulate as a porous particle bed atop the crust. The third mechanism is mechanical breach of a suspended crust. In particular, the thick crusts that form from water ingress could bond to the reactor cavity walls, eventually causing the melt to separate from the crust as the MCCI continues downwards. This phenomenon is caused by limestone volume shrinkage due to decarbonation (loss of more than 40% of mass and similar densities of CaO and CaCO3). However, this configuration is not expected be mechanically stable due to the poor mechanical strength of the crust in comparison to the applied loads (i.e. the crust weight itself, plus the weights of the overlying water pool and the accumulating dispersed material minus the
pressure of concrete decomposition gases). Eventually the suspended crust will fail, leading to rapid ingression of water beneath the crust. This sudden introduction of water will provide a pathway for renewed debris cooling by the bulk cooling, water ingression, and melt eruption cooling mechanisms.

Debris cooling can be substantially enhanced by a large-scale breach of crust that is formed at the melt water interface. It is stipulated that an anchored crust (i.e. a crust formed at the melt-water interface that is attached to the sidewall and otherwise suspended along the span) is likely to fail at the plant scale because of its low structural strength. The presence of extensive cracks in the crust (see Figure 1.3-3 as an example) contributes, in large part, to such low strength. Such a crust is expected to fail under the applied load (namely, the combined weight of the crust, overlying particle bed, and water pool minus the pressure of concrete decomposition gases) at the plant scale, and to provide pathways for significant water ingression through macroscopic breaches and, as a result, will provide additional cooling. The extent to which these various mechanisms can be effective in terms of thermally stabilising the molten core debris is principally a function of melt depth and the timing of water addition into the cavity. Section 1.5.3 provides more information about this important safety issue.

![Figure 1.3-3: Extensive crack structure in the mid-plane sample of the SSWICS-3 crust](image)

1.4. Relevant containment failure modes due to ex-vessel corium behaviour

Ex-vessel corium poses a number of threats to the containment integrity as long as the melt stabilisation is not achieved. This section provides a simple description of the most relevant containment failure modes arising from core concrete interactions.

1.4.1. Basemat melt-through

Basemat melt-through is a late containment failure mode. This late containment failure mode occurs when the coolability of the corium accumulated in the reactor cavity floor cannot be achieved. Under these conditions, the continuous axial concrete erosion by the corium eventually results in the basemat perforation with possible ground and water contamination.

As mentioned in Section 1.3.3.1, in addition to the axial concrete ablation, radial concrete erosion should be also kept in mind. Fast radial ablation could also perforate cavity walls, leading to the spreading of corium into adjacent rooms, e.g. instrumentation room. Whereas corium spreading is favourable to its stabilisation, the presence of plant specific features such as drains, sumps etc. could result in additional containment weakness. Also the thermal weakening and possible long term deformation of load-carrying inner structures should be considered.
In the previous description of the basemat melt-through as a late containment failure, the term “basemat perforation” should be understood as the perforation of the containment liner located below the concrete slab within the reactor cavity. However, some nuclear power plants are designed with additional concrete slab below the liner. This additional concrete slab could provide an additional barrier to prevent the ground or water contamination. The credit of this concrete thickness slab as a safety measure is a plant specific issue that should be studied very carefully.

Realistic timing for the basemat melt-through usually ranges from one to several days after the onset of the severe accident. This timing depends on many factors: plant specific features, such as configuration of the reactor cavity; concrete type; severe accident sequence characteristics, i.e. amount of corium discharged from the RPV, level of residual power, metal content of the corium, the presence of water in the reactor cavity (either before the RPV failure or by pouring water atop the corium after RPV failure), etc.

A great amount of research and analytical activities have been devoted to studying this late containment failure mode for many years. These activities have provided an appropriate understanding of the phenomena involved in corium-concrete interactions and the necessary information to determine the feasibility of a number of severe accident strategies aimed at preventing this late containment failure mode. Moreover, the core content of this SOAR is heavily focused on these two severe accident phenomena. Section 1.6 provides information about the safety importance of these phenomena within the field of the severe accident.

1.4.2. Containment failure by over-temperature

Containment system consists of several elements. In addition to the primary structure, these include various components such as the mechanical (hatches, piping) and electrical (instrumentation and controls) penetrations. There are a large variety of designs for these components and not all can be tested. Nevertheless, a series of tests of a variety of containment penetrations types has been conducted in order to determine their leakage characteristics under severe accident conditions (Hessheimer M.F., 2006).

As an example of this kind of containment failure mode, severe accident analyses conducted on boiling water reactor (BWR), equipped with Mark I type of containment, have indicated very high temperatures in the drywell region as a consequence of the molten core concrete interactions (Claus, 1989) (NRC, 1990), (Herranz & al., 2012). Drywell region is the location of the electrical penetration assemblies and the drywell head.

On the basis of the aforementioned experimental series, it could be concluded the loss of the containment integrity due to the high temperatures within drywell as a result of the core concrete interactions.

The SOARCA study for a BWR equipped with a Mark I type of containment (SNL, 2012) and (Gauntt, et al., 2012) describes a potential failure of a containment mechanical penetration, namely the drywell head flange. A short description of this potential failure of the drywell head flange is provided below. Note, that drywell head flanges for other BWR plants may be different, so that the conclusions of these studies could not be applicable to other plants.

Figure 1.4-1 shows an example of a drywell head flange. Note that the drywell head is removed during refuelling to gain access to the reactor vessel. According to Figure 1.4-1, the drywell head flange is connected to the drywell shell with bolts. The flanged connection also has gaskets. Gaskets are replaced during each reassembly of the reactor vessel head, because they are exposed to constant temperature and radiation, which contribute to their early degradation.

The drywell head flange bolts are pre-tensioned during reassembly of the head. This pre-tension also compresses the gaskets in the head flange.
During accident conditions, the containment vessel may be pressurised internally. The internal pressure would then begin to counteract the pre-stress in the bolts. When the internal pressure produces counter-stress equivalent to the bolt pre-stress, elastic strain of the head bolts will occur, allowing a small gap to open up between the mating surfaces of the flange. Further increase in the internal pressure would result in leakage at the flanged connection. Because the bolt stress is in the elastic range of the stress-strain curve, when the containment pressure decreases, the gap reduces as the bolt contracts and the leakage area is thereby reduced. In reality, some permanent strain may take place in the flange region and seal degradation may also take place such that the leakage area may not reduce to zero as the containment pressure decreases.

Figure 1.4-2 shows the leak area of the drywell head flange vs. drywell pressure. Figure 1.4-2 also contains the results obtained by the parametric analyses performed for this analysis. The selected uncertainty parameters were: the modulus of elasticity (E) of the drywell head bolts, the torque coefficient associated with the pre-tensioning of the head bolts (K), and the rebound thickness of the drywell head gasket. More detailed information for this uncertainty study is included in (SNL, 2013).
1.4.3. Failure of the reactor pressure vessel pedestal in BWR

The reactor pressure vessel (RPV) is supported by the reactor pedestal in BWR. Failure of the pedestal will result in gross motion of the RPV that could lead to the containment failure under specific circumstances. A number of phenomena could lead to the failure of the pedestal, e.g. direct containment heating, the occurrence of an ex-vessel steam explosion, the reactor pressure blowdown from high pressure or the concrete erosion by molten core. Only the last mechanism will be considered in this section. (Payne & al., 1990); (Brown, y otros, 1990); (Breeding, y otros, 1991) and (Harper, 1991) address this safety issue. The following discussion is focused on BWR equipped with Mark III type of containments.

Pedestal failure is defined as the loss of support of the RPV such that gross motion of the vessel results. If the vessel fails, core debris is released into the reactor cavity. Assuming the debris is not coolable, it will participate in molten core-concrete interaction. During molten core-concrete interaction, both the concrete and the extensive mesh of rebar in the pedestal region are eroded. If the erosion into the pedestal wall is extensive, the pedestal may collapse from the load imposed by the RPV and the shield wall. As previously said, failure of the pedestal will result in gross motion of the RPV. Several large pipes are attached to the RPV that penetrate the drywell (e.g. steam lines and feedwater lines). The motion of the RPV, and hence the motion of these pipes, can damage the penetrations and fail the drywell boundary. The integrity of the drywell boundary can also be impaired by damage of the drywell liner that results from the RPV motion. The combination of these events can establish pathways that bypass the suppression pool. Relevant events, variables and phenomena involved in the potential failure of the pedestal are: the presence of water in the cavity floor, the amount of corium discharged from the RPV and the flow rate, corium superheat and amount of unoxidised metal in the molten corium and decay heat.

1.4.4. Mark I containment failure by melt-attack of the liner

This early containment failure mode is specific of BWR equipped with a Mark I type of containment. In this type of containments, relocation of molten core debris into the drywell cavity pedestal region occurs upon RPV failure in a low-pressure severe accident. The liner failure issue arises because the tight containment floor geometry relative to the large core inventory can lead to molten core debris flowing a short distance and contacting the drywell liner. Failure of the containment liner shell at this location could lead to rapid blow down of the drywell atmosphere into the reactor building and subsequently into the environment without the benefit of the suppression pool scrubbing.

(Theofanous, Yan, & Podowski, 1993) provide with the details of the resolution of this important safety issue. This study shows that flooding the drywell with water significantly reduces the thermal load on the drywell liner to the point at which liner failure can be highly unlikely (conditional probability less than 10-3 given a core melt accident). Substantial amount of water in the drywell could be achieved by maintaining operational the drywell sprays throughout the severe accident, what in turn underscore the importance of the implementation of appropriate severe accident management strategies.

1.4.5. Coolability of the widely dispersed debris into the containment

In some cases, severe accident progression might result in the widespread debris dispersal into the containment. This situation could occur in case of high pressure melt ejection, steam explosions, vessel rocketing etc. The widespread debris dispersal might form a film on the containment boundary structures with potential to challenge the containment integrity. This safety issue was analysed in (NRC, 1990), (NRC, 1993) and (Harper, 1994) concluding that very little was known at that time about the expected corium distribution under the previous energetic events. Nevertheless, if corium comes to rest in a thin uniform layer air cooling will suffice. On the other hand, it is possible that drifts of corium particles might accumulate in corners, in the wall-floor angle, and so on, that would be
enough to reheat and start core concrete interaction. Debris coolability was considered as very uncertain in such scenarios.

(Alsmeyer H., et al., 1995) also highlighted the safety concerns raised by the corium dispersion in the containment, including the possibility of a delay loss of containment integrity.

The GAREC working group was also concerned with the problem of corium dispersion in the containment (Seiler, et al., 1999).

Taken advantage of two plant specific experiments conducted for two PWR designed by Westinghouse, (Hammersley, Cirauquib, Faigc, & Henrya, 1996) provides experimental evidence for the potential of liner ablation when it gets in contact with the debris dispersed throughout the containment. Due to a plant specific design feature in one of these two PWRs, there exists a possibility of direct contact between the liner and the dispersed debris in the containment annular compartment that might lead to a loss of the containment integrity.

These two experiments were conducted with stimulant materials and the containment liner was simulated with a liner plate made of stainless steel. Liner plate was directly impacted by molten debris in both experiments and once removed the solidified deposits and inspection of the liner plate surface, no ablation or degradation was observed. Additionally, these two experiments provided the basis for the analytical work shown in (Robledo & Lantaron, 1999), aimed to determine the needed conditions leading to the liner failure by deposits on the liner.

(Meyer, Albrecht, Caroli, & Ivanov, 2009) show the most relevant findings obtained in the integral tests performed in DISCO-H facility employing an iron – alumina melt as corium stimulant, steam in the RPV and a prototypic atmosphere in the containment.

As for the potential impact on the containment integrity of the widely particle debris dispersed into the containment, (Meyer, Albrecht, Caroli, & Ivanov, 2009) provide the following information:

- Tests with EPR geometry.
- “Debris in the DISCO vessel was recovered from (…) the dome surface and structures inside the containment. (…) The debris in the containment was generally collected as small particles on the wall, the top cover and on the floor”.
- “The particles larger than 5 mm are generally flat, because they hit a wall when they are still liquid, the particles smaller than 2.5 mm have a spherical shape with a cavity in the centre”.
- Test with VVER-1000 geometry.
- “Less than 2% of the initial melt mass reached the containment as fine dust, with a median diameter of ≈ 0.15 mm”.
- Tests with Konvoi geometry.
- “The debris in the containment was generally collected as small particles on all horizontal areas, such as the subcompartment covers and hoses. (…) Only very fine particles could enter the containment dome, here the mean diameter is 0.08 mm. These particles probably were already solid when they entered the containment dome”.

1.4.6. Containment over-pressurisation

When the RPV fails at low pressure, the debris pours into the cavity and accumulates at the bottom of the cavity. In the absence of corium cooling by water, the interaction of the corium with the cavity concrete basemat results in ablation of the concrete substrate and the subsequent generation of non-condensable gases, namely, steam, H2, CO2 and CO. As shown in Table 1.3.4, the quantities of steam...
and CO2 released vary with the concrete type. Note that basaltic and siliceous concrete contains much less CO2 gas than LCS concrete.

In addition to the containment basemat melt through, concrete ablation and the subsequent non-combustible gas generation could lead to two additional potential containment failure modes.

As explained in Section 1.3.3, H2O and CO2 released from the concrete could oxidise the metals contained in the corium. These exothermic chemical reactions have a twofold effect: increase the melt temperature and generate flammable gases: H2 and CO, in addition to the H2 generated during the in-vessel phase. The potential combustion of H2 and CO generated throughout a severe accident could lead to a significant pressure peak in the containment, threatening its integrity. In addition to the implementation of severe accident management guidelines (see Section 1.5) a number of safety systems (e.g. passive autocatalytic recombiners (PAR’s), igniters, inerting the containment atmosphere, etc.) have been designed and implemented in many LWRs in order to prevent this containment failure mode (OECD/NEA, 1997), (IAEA, 2001).

The second possible containment failure mode is the so-called containment slow over pressurisation. When the containment heat removal systems are inoperable, containment would fail by slow pressurisation due to the build-up of the steam and non-condensable gases generated during the ex-vessel phase of a severe accident. Under these circumstances, containment failure by slow over-pressurisation is a late containment failure mode, because it takes place several hours after the RPV failure.

However, containment venting could be activated in Mark I containments before the RPV failure in some severe accident scenarios. This safety issue is analysed below. In order to prevent this containment failure mode, filtered containment venting systems have been developed and implemented in many NPPs (IAEA, 2001), (OECD/NEA, 1988).

(NRC, 2012) provides examples of severe accident scenarios where the containment venting system could be activated even before the RPV failure. These scenarios are applicable for BWR with a Mark I containment. Taking advantage of the SOARCA study (SNL, 2012), NRC has modelled with MELCOR a number of both long-term and short-term station blackout (SBO) for Peach Bottom leading to one of three possible outcomes: containment overpressure failure, liner melt-through failure, or maintaining the containment intact as a result of venting or other mitigation measures. In modelling a SBO, it is assumed that low-pressure core injection (LPCI), high pressure core injection (HPCI), drywell spray, core spray and other engineered safety features (ESF), normally designed to run by AC power, become unavailable for an extended period of time. However the reactor core isolation cooling (RCIC) system is assumed to be running for 16 hours, because the RCIC is designed to provide core cooling, the core uncover and the subsequent accident progression is delayed accordingly. The RCIC operation is controlled by battery, which acts as a power source for control valves that run the RCIC pump on and off. The rationale followed to consider the RCIC to be running for 16 hours are provided below.

The SOARCA study assumed RCIC operation for 4 hours. Many, if not most, USBWR Mark I plants are equipped with batteries that will allow RCIC to run for an extended period of as much as 8 hours. Moreover, in the post-9/11 development of accident management strategies, conceivably even a longer battery life for RCIC operation may have been considered. In Fukushima Daiichi Unit 2, RCIC operation in excess of 70 hours has been reported although the reason for such an extended operation is yet unknown. Likewise, in Fukushima Daiichi Unit 3, RCIC operation on the order of 20 hours has been reported followed by another 16 hours of HPCI operation that kept the core cooled. With these considerations in mind, RCIC operation of 16 hours has been assumed in the MELCOR calculations considered in this section.
The BWR Emergency Procedure Guidelines (BWREPGs), which form the basis for plant specific emergency operating procedures, contain provisions for containment venting through the wetwell and drywell (section 1.5 contains more detail information about EPGs and SAMGs). The primary function of venting is to prevent containment failure by overpressure from steam and other non-condensable gases. The BWR Mark I plants were originally designed with wetwell vents that had a low pressure capacity. As a result of post-TMI improvements, the wetwell vents in many of these plants have been upgraded and “hardened” for a high pressure capacity. Note that containment venting through the wetwell has the advantage of attenuating fission products through suppression pool scrubbing. Wetwell venting is activated when the containment pressure exceeds 60 psig (4.1 bar gauge) in the cases analysed here.

(NRC, 2012) describes the results obtained by running 30 SBO scenarios with MELCOR for Peach Bottom. Three cases were selected for this section. It has been respected the original nomenclature for the cases contained in (NRC, 2012).

The three selected cases are briefly explained below:

- **Case 2.** SBO with total loss of safety systems, except for the RCIC. RCIC keeps running for 16 hours.

- **Case 6.** Same as case 2, but core spray is activated after the RPV failure. The rationale to consider the activation of the core spray is provided next. Upon termination of RCIC operation, a portable device, namely a diesel generator driven fire water system, was considered to feed the low-pressure core spray system but only after RPV depressurisation. A 300 gpm flow rate for the core spray was used in this case 6.

- **Case 14.** Same as case 2, but drywell spray is activated at 24 hours. Another mitigation feature considered in the current study is drywell spray with a nominal flow rate of 300 gpm. As in the case of core spray, the drywell spray is assumed to be operated by a diesel-powered portable device. The drywell spray is actuated at 24 hours which, in most cases, correspond to the timing of RPV lower head failure. A nominal flow rate of 300 gpm for drywell spray was considered.

MELCOR results for these three cases are shown in Table 1.4-1. In addition, Figure 1.4-3 shows the containment pressure evolution for the three cases.

**Table 1.4-1:** Timing of key events for MELCOR calculations

<table>
<thead>
<tr>
<th>Event timing hr</th>
<th>Case 2</th>
<th>Case 6</th>
<th>Case 14</th>
</tr>
</thead>
<tbody>
<tr>
<td>SBO</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
</tr>
<tr>
<td>RCIC terminates</td>
<td>17.9</td>
<td>17.9</td>
<td>17.9</td>
</tr>
<tr>
<td>Active fuel uncovery</td>
<td>22.9</td>
<td>22.9</td>
<td>22.9</td>
</tr>
<tr>
<td>Drywell pressure &gt; 60 psig (4.1 bar gauge) – vent opens if applicable</td>
<td>22.8</td>
<td>23.3</td>
<td>23.2</td>
</tr>
<tr>
<td>First Hydrogen production</td>
<td>23.6</td>
<td>23.6</td>
<td>23.2</td>
</tr>
<tr>
<td>Relocation of core debris to lower plenum</td>
<td>25.9</td>
<td>25.9</td>
<td>25.7</td>
</tr>
<tr>
<td>RPV lower head fails grossly</td>
<td>37.3</td>
<td>36.7</td>
<td>36.6</td>
</tr>
</tbody>
</table>
1.4.7. **Safety issues unaddressed by this report**

MCCI SOAR focuses heavily on molten core concrete interactions and the cooling mechanisms for the corium discharged from the RPV. As a result, a number of severe accident phenomena associated with the ex-vessel phase of a severe accident are beyond the scope of the present SOAR. Below are listed the safety issues unaddressed by this report.

- Corium jet impingement against concrete.
- Fuel coolant interaction after RPV failure.
- Steam spike.
- Melt jet break up.
- Debris bed coolability.
- Melt spreading.
- Fission products release.
- Recriticality.
- Thermal stresses on concrete structures brought on by core debris interactions.
- Consequence of basemat penetration in terms of core debris migration into ground and fission products release by leaching process with ground water.

Although this report focuses on corium-concrete interaction issues, one has to acknowledge that valuable information can be found also in the works published by the nuclear or fire community to address the concrete degradation due to surface heating by radiative heat flux (Chu T., 1978) or all the work performed on sodium-concrete interactions.

**Figure 1.4-3:** Comparison of drywell pressures for selected cases
1.5. Severe accident management issues and back-fitting measures

Accident management is essential to ensure effective defence in depth. (IAEA, 2009) provides the following definition of accident management:

“Accident management is the taking of a set of actions during the evolution of a beyond design basis accident:

- To prevent the escalation of the event into a severe accident;
- To mitigate the consequences of a severe accident;
- To achieve a long term safe stable state.

The second aspect of accident management (to mitigate the consequences of a severe accident) is also termed severe accident management. Accident management is essential to ensure effective defence in depth at the fourth level. According to the (IAEA, 2009), “the objective of the fourth level of defence in depth is to ensure that both the likelihood of an accident entailing significant core damage (a severe accident) and the magnitude of a release of radioactive material following a severe accident are kept as low as reasonably achievable and, thereby, to reduce risk”.

(OECD/NEA, 1995) provides the following definition of severe accident management:

“Severe Accident Management consists of those actions that are taken by the plant staff during the course of an accident to:

- Prevent core damage.
- Terminate progress of core damage and retain the core within the vessel.
- Maintain containment integrity, and
- Minimise off-site releases.

Severe accident management also involves pre-planning and preparatory measures for:

- Severe accident management guidance and procedures.
- Equipment modifications to facilitate procedure implementation, and
- Severe accident training.

The overall objective is to further reduce the risks of large releases. It is the responsibility of the licensees to develop and implement a severe accident management.”

This definition includes the concept that there is some overlap between what is referred to as accident management and severe accident management.

Accident management guidance has different forms depending on the considered domain. The guidance for the preventive domain, therefore, takes the form of procedures, usually called emergency operating procedures (EOPs), and is prescriptive in nature. EOPs cover both design basis accidents and beyond design basis (IAEA, 2009).

In the mitigatory domain, uncertainties may exist both in the plant status and in the outcome of actions. Consequently, the guidance for the mitigatory domain should not be prescriptive in nature but rather should propose a range of possible mitigatory actions and should allow for additional evaluation and alternative actions. Such guidance is usually termed Severe Accident Management Guidelines (SAMGs) (IAEA, 2009).
The guidance should contain a description of both the positive and negative potential consequences of proposed actions. The guidance for the mitigatory domain should be presented in the form of guidelines, manuals or handbooks. The term “guideline” here is used to describe a fairly detailed set of instructions that describe the tasks to be executed on the plant, but which are still less strict and prescriptive than the procedures found in the EOPs; that is, used in the preventive domain. Manuals or handbooks will contain a more general description of the tasks to be executed and their background reasoning.

Over the past fifteen years many of the nuclear power plants worldwide have been equipped with a capability for severe accident management. This has been driven partly by the SAMGs developed by owners groups for plant specific applications.

The impact of SAMGs has also been included in a number of Level 2 PSA studies which resulted in a revision of core damage frequency, large early release frequency, and source term estimates. A review was performed to identify the new developments and insights derived from Level 2 PSA activities worldwide in the past ten years. Much of this new information was presented at the two CSNI Workshops – the first on Level 2 PSA and Severe Accident Management held in Köln in March 2004, and the second on the Evaluation of Uncertainties in Relation to Severe Accidents and Level 2 PSA held in Aix-en-Provence in November 2005.

After this period, it was clearly recognised that Level 2 PSA approach is now a common practice for most reactors as explained in a 2007 CSNI-WG-Risk Technical Opinion Paper (TOP) on L2PSA established after these two Workshops.

“The main message of this TOP is that the Level 2 PSA methodology may now be seen as mature. This is reflected by the large number of high quality analyses that have been performed in recent years and used to identify the potential vulnerabilities to severe accidents and the accident management measures that could be implemented.

The Level 2 PSA is now seen as an essential part of the safety analysis that is carried out for all types of nuclear power plants worldwide. The information provided by the Level 2 PSA is being used by plant operators and Regulatory Authorities as part of a risk informed decision making process on plant operation and more specifically on issues related to severe accident management.”

Nevertheless, it was also recognised that some discrepancies may exist in L2 PSA practical methodologies, for example to introduce uncertainties associated to the severe accident phenomena. After having promoting exchanges in the framework of the EC-SARNET, the European Commission has sponsored the ASAMPSA2 project (ASAMPSA2, 2013) which has established some detailed best-practices guidelines for L2 PSA development and applications, taken into consideration recent results from research area. These guidelines summarise available knowledge on MCCI issues in 2011. They will be update by the ongoing EC-ASAMPSA_E project (www.asampsa.eu).

Finally, the European Commission (EC) sponsored two studies: SAMIME (Severe Accident Management Implementation and Expertise in the European Union) and SAMOS (the viability of using computerised aids to assist in severe accident management) to understand the severe accident management practice in member states and to investigate the feasibility of computerised tools for accident and emergency management.

A brief description of the severe accident management strategies aimed to maintain containment integrity for present LWR after the corium is poured onto the reactor cavity floor are described below.

1.5.1. Dry cavity and no water addition

As it can be seen in Figure 1.2-1, when core concrete interaction proceeds under dry conditions, late containment failure is certain for most of the cases.
1.5.2. Reactor cavity flooded before RPV failure at low pressure

When the cavity is flooded before the vessel breach, there exist chances of formation of coolable debris, especially with deep water pools. Available space for sufficiently deep pools depends on plant design and is only available in some present LWR. With limited space in other types of reactors only partial cooling and melt retention after a RPV failure can be expected from cavity flooding. As possible counter-effects, the occurrence of steam explosions or strong steam spikes is considered in Figure 1.2-1. Note that water has to be continuously added in order to achieve long term stabilisation. Otherwise, only some delay for the basemat melt-through could be gained.

Steam explosions are beyond the scope of this report (see Section 1.4.7). The term steam spike refers to pressure generated due to rapid quenching of the corium melt. For this process to occur the melt must come into intimate contact with substantial quantities of water (NRC, 1985). The steam spike phenomenon occurs when the molten debris is ejected from the reactor vessel and it encounters water in the cavity or in the containment floor after being swept out of the cavity under high pressure. The immediate corium/water interaction would generate a large amount of steam and produce a pressure spike in the containment.

(Yang, 1990) provides very detailed information about the main results obtained by the NRC during its investigations on the steam spike issue for American large dry PWR containments. Based on these analyses, (Yang, 1990) concludes that the failure of the PWR large dry containment by a steam pressure spike at the time of vessel failure is an event of relatively low probability. Note, that this conclusion is applicable only for those PWR within the scope of the aforementioned study.

1.5.3. Water injection into the reactor cavity after RPV failure at low pressure

According to Figure 1.2-1, the branch without water in the cavity at the time of melt release is considered to yield a manageable situation (i.e. termination of basemat erosion before melt-through or over-pressurisation of the containment), only in case that by flooding of the cavity with water finally a coolable configuration is established. Section 1.3.4 describes the various corium cooling mechanisms, when water is poured onto the corium. Section 1.3.4 also states that their efficiency in terms of thermally stabilising the molten core debris is principally a function of melt depth and the timing of water addition into the cavity. Analytical activities supporting this statement are shown below.

The computer code CORQUENCH was developed within the frame of the MACE, NEA-MCCI and MCCI-2 experimental projects. CORQUENCH code contains phenomenological models for the ex-vessel corium cooling mechanisms, along with other models such as material properties and concrete ablation models. Chapters 2 and 3 provide, respectively, detailed information about the experimental programmes and the models contained in CORQUENCH.

(Robb & Corradini, 2009) show the results obtained in a sensitivity study performed with CORQUENCH, whose main objective was to investigate the importance of system initial and boundary conditions and also cooling phenomena to the progression of MCCI.

This sensitivity study was carried out over a range of input conditions for two concrete compositions: LCS and siliceous. A 2D right cylindrical geometry was used to simulate reactor cavity scale accidents and allowed for 2D concrete ablation. Note that the melt pool can only be modelled as homogeneous in CORQUENCH. The input parameters and the range over which they were varied are noted in Table 1.5-1.
Table 1.5-1: Input parameters varied in the 2D initial and boundary condition sensitivity studies

<table>
<thead>
<tr>
<th>Parameter</th>
<th>2D Analysis Range</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cavity Pressure</td>
<td>0.1-0.5</td>
<td>MPa</td>
</tr>
<tr>
<td>Water Add Time Delay</td>
<td>5-120</td>
<td>min.</td>
</tr>
<tr>
<td>Time Since Reactor Shutdown</td>
<td>1-8</td>
<td>hrs.</td>
</tr>
<tr>
<td>Melt Initial Temp</td>
<td>2400-2900</td>
<td>K</td>
</tr>
<tr>
<td>Melt Initial Mass</td>
<td>45-180</td>
<td>tonne</td>
</tr>
<tr>
<td>Zr Initial Oxidation</td>
<td>20-80</td>
<td>w%</td>
</tr>
<tr>
<td>Cavity Diameter</td>
<td>5-10</td>
<td>m</td>
</tr>
</tbody>
</table>

This sensitivity study shows that with respect to initial and boundary conditions, the initial melt mass and the cavity diameter are consistently the most important independent variables with respect to ablation. Further investigation into sensitivity studies show that the initial collapsed melt height, dependant from the above mentioned independent variables, is the most important initial condition. The time delay of water addition was second in importance with the other independent variables having tertiary importance.

As for the 2D modelling phenomena sensitivity studies, Table 1.5-2 shows the input parameters considered. The range of values is based on post-test experimental data from the MACE test program. Constant values for the melt entrainment coefficient, crust permeability, and crust thermal conductivity were used instead of the phenomenological models available in CORQUENCH.

The main outcomes of this sensitivity study are shown below.

As for LCS concrete, the melt entrainment coefficient has a dominant effect on the total radial and axial ablation, H2 gas generation, as well as the ratio of axial to radial ablation. The crust permeability was second in importance followed by the other parameters.

Table 1.5-2: Input parameters varied in the 2D modelling phenomena sensitivity studies

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Range</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Particle Bed Porosity</td>
<td>33-59</td>
<td>%</td>
</tr>
<tr>
<td>Particle Bed Diameter</td>
<td>1.4-4.2</td>
<td>mm</td>
</tr>
<tr>
<td>Parameter</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Melt Entrainment Coef.</td>
<td>0.00-0.0025</td>
<td>-</td>
</tr>
<tr>
<td>Crust Permeability</td>
<td>2.5-50 x10^{-11}</td>
<td>m²</td>
</tr>
<tr>
<td>Crust Thermal Conductivity</td>
<td>1.25-1.75</td>
<td>W/m K</td>
</tr>
<tr>
<td>Crust Strength</td>
<td>0.2-10</td>
<td>MPa</td>
</tr>
</tbody>
</table>

As for siliceous concrete, the melt entrainment coefficient was also the most influential parameter to melt coolability, whereas the crust strength was now second in importance.
Note that the above-described analytical results are dependent upon the models used in CORQUENCH and may only apply within the ranges of the parameters investigated. As more accurate phenomenological models are crafted and implemented, new sensitivity analysis should be accomplished for.

1.5.4. Back-fitting measures in Gen. II reactors aimed to prevent the basemat melt-through

Recently, modifications have been implemented in the French Fessenheim Gen II reactor in order to prevent early basemat melt-through. Chapters 5 and 6 provide more detailed information about these back-fitting measures that are briefly enumerated below:

- To increase the corium spreading area. In order to achieve it, dedicated holes in the vertical walls of the reactor cavity could be drilled allowing for corium spreading into rooms adjacent to the reactor cavity.
- To increase the thickness of the concrete basemat slab.

1.5.5. Generation III reactors

New reactor designs are expected to have enhanced capabilities both for preventing and for mitigating severe accidents. However, severe accident management strategies remain an important element of defence-in-depth for these new designs.

The new designs include safety enhancements not present in existing plants aimed at achieving melt stabilisation. It basically could be distinguished between two classes of concepts:

- Concepts based on enclosure of melt and external cooling: enclosure concepts.
- Concepts relying on formation of particulate or porous debris as a basis for rapid quenching and cooling due to surface increase.

Figure 1.5-1 gives an overview of the different concepts and also indicates advantages and disadvantages. Here, measures are indicated from the side of constellations resulting from measures and scenarios (left side) as well as from major retention and cooling concept (right side) and cooling concepts (right side) and their requirements.

There exist certainly possibilities of combinations of basic goals and respective measures, partly already depicted in Figure 1.5-1 i.e. some surface increase of the melt is provided by measures of spreading of melt on a dedicated large area, as in the EPR concept (Fischer, 1999), (Seiler, et al., 2003). However, since in this concept the melt layer may remain too thick (≈ 40 cm.) for removal of decay heat in solid state, molten parts can exist over weeks or months, until the decay heat is sufficiently reduced. This is because, at high decay power and without crediting melt fragmentation and water ingress, heat can only be effectively transported to the cooled outer surfaces when the melt remains in a partly liquid state. This means that the cooling from bottom and top which ensures the enclosure of the melt inside crusts must be maintained over several months.
The Tian-Wan core catcher concept (Seiler, et al., 2003), (Sidorov, 2000) represents another enclosure concept, with a hot melt pool enclosed by crusts within a large steel vessel beneath the RPV. Retention in liquid state has to be guaranteed over an even longer time by external water cooling of the catcher vessel. Heat removal is enabled by diluting the melt with sacrificial material in the catcher vessel, thus reducing the power density and increasing the surface for heat removal. As for ESBWR, the BiMAC device is intended to arrest core melt progression in the lower drywell by cooling the debris both from above and below (Gavrilas & Fuller, 2010).

The major concepts based on fragmentation and porosity formation are the deep water pool (Seiler, et al., 2003), (Chu, Sienicki, Spencer, Frid, & Löwenhielm, 1995), (Lindholm, Holmstroem, Miettinen, Lestinen, & Hyvaerinen, 2006) in the cavity yielding melt break-up and particulate debris formation as well as the COMET concept (Alsmeyer & Tromm, 1999), (Alsmeyer, et al., 2004) providing a coolable porous layer by means of water injection from the bottom into a spread layer. Rapid quenching occurs in both concepts due to a large melt-water surface increase. It is further supported by co-current flow modes of water and steam, especially in the COMET concept, provided by the bottom injection of water.

A combination with melt spreading as in the EPR concept is to some extent essential also for the COMET concept. The presently favoured design is optimised with respect to a relatively small layer thickness of about 40-50 cm (as in the EPR concept). Easier implementation of the device is envisaged with the COMET PC(A) concept (Alsmeyer & Tromm, 1999), where the water is injected via porous concrete instead of specific injection nozzles. The water filled porous concrete may also be considered to yield an additional protection as an enclosure.

Partly, the functioning of such concepts may be affected by concrete erosion, e.g. concerning conditioning of the melt by mixing with sacrificial material or metallic/ceramic layering during a

---

4. On the right side of the figure (FP retention and coolability), melt surface has to be understood as melt-water exchange surface.
phase before termination of erosion by stable enclosures or water injection and quenching. Further a failure of a retention concept might occur. The analyses of possible weaknesses of such concepts and of continued erosion processes after a failure still requires reliable knowledge about concrete erosion, i.e. not only for existing types of reactors but also for considerations on new concepts.

The core melt stabilisation system (CMSS) and the Severe Accident Heat Removal System (SAHRS) in EPR are examples of features designed to ensure core debris coolability. Numerous features are incorporated into the ABWR design to help mitigate the effects of core concrete interaction. These are: a large lower drywell flooder (LDF) system, an AC-independent water addition (ACIWA) system, use of sacrificial basaltic concrete for the lower drywell floor, and a Containment Overpressure Protection System (COPS). Ex-vessel melt stabilisation is included for other new reactor designs as well including VVER-1000, APWR, and CANDU. In some cases, different strategies from those used for existing reactors must be adopted.

All the above mentioned new safety features and accident management strategies have used insights derived from two decades of molten core-concrete interaction and ex-vessel debris coolability research findings.

1.6. Prioritisation of severe accident research issues

Once obtained a global view of the severe accident phenomena addressed by this SOAR, basically, corium-concrete interactions and ex-vessel corium coolability mechanisms by top flooding, it is time to highlight the safety significance given to these severe accident issues by the scientific and technical community.

(Magallon, et al., 2005), (Schwinges, y otros, 2008) and (Klein-Heßling, et al., 2012) contain detailed information on the activities carried out by the European Community and SARNET to prioritise all the severe accident issues. This activity was deemed necessary to guarantee that the research conducted on severe accidents remains efficient and conveniently centred. Before describing these activities in detail, the main conclusions obtained in these decision processes related with the matters addressed by this SOAR will be explained.

Three safety prioritisations for severe accident issues have been conducted by the Severe Accident Research Priority (SARP) group and all of them concluded that the two following severe accident issues should have the top safety priority, e.g. high.

- MCCI: molten pool configuration and concrete ablation.
- Ex-Vessel corium coolability, top flooding.

More detailed explanations about this decision process are provided below.

On 2001, the European Union considered that a work methodology aimed at ensuring that the research conducted on severe accidents is efficient and well-focused should be developed and implemented. As a result, it was created the EURSAFE Project within the FP5 (Magallon, et al., 2005). EURSAFE accomplished the first PIRT (Phenomena Identification and Ranking Table) for severe accidents. It integrated all the severe accident issues from core degradation up to release of fission products from the containment, taking into account any possible countermeasures and the evolution of fuel management. Two evaluations were established:

1. The safety importance ratio.
2. The knowledge ratio.

Starting with 1 016 identified phenomena, the list was reduced to 239 items important for safety, of which 106 were found with significant lack of knowledge. These items have been summarised to 21 items, the so-called EURSAFE Research Issues (ERI). Two of these ERI were:
1. MCCI: molten pool configuration and concrete ablation

The research within this ERI would be aimed at improving the predictability of axial vs. radial ablation up to late phase MCCI to determine basement material failure time and loss of containment integrity.

2. Ex-vessel corium coolability, top flooding

The research within this ERI would be aimed at increasing the knowledge of cooling mechanisms by top flooding the corium pool to demonstrate termination of accident progression and maintenance of containment integrity

The SARNET Severe Accident Research Priority (SARP) group, within SARNET FP6, accomplished a thorough review of this first PIRT four years later (Schwinges, et al., 2008). The main conclusions reached by the SARP group were:

- 6 out of these 21 ERI were still considered with high priority.
- 4 ERI were re-assessed with medium priority
- 5 ERI were re-assessed with low priority.
- 3 ERI were marked as “issue could be closed”.

The two ERI concerned with this report, i.e. MCCI: molten pool configuration and concrete ablation and ex-vessel corium coolability, top flooding, held their previous safety prioritisation: high.

Four years later, a new reassessment of the severe accident issues prioritisation was conducted by the SARNET Severe Accident Research Priority (SARP) group within SARNET FP7 (2009-2013). (Klein-Heßling, et al., 2012) describe this activity in detail, whose more relevant conclusions are explained below.

In order to correctly address this problem, it was deemed that the organisations involved in the working group should be selected from researchers, members of Technical Safety Organisations (TSO) and utilities. Thus, the SARP work package was comprised by the following 12 organisations: IRSN, Areva, CEA, EDF, KIT, GRS, JSI, KTH, PSI, RUB, TUS and VTT. GRS was the leading organisation.

Relevant sources of information for this decision process were:

- The results of the ongoing SARNET work-packages and the related research projects and experimental programmes.
- The results of the finished ASAMPSA2 project.
- The results of NEA projects (such as MCCI, THAI and BIP).

Although the procedure for the decision making was similar to that used in the previous SARP works, some minor differences were introduced. The vote of phenomena was considered first before discussing the safety relevance. The scope of the “safety-oriented vote” was widened. In addition to the radiological consequences of a severe accident, the consequences on accident management measures and procedures were also taken into account. Moreover, this reassessment also considered a possible change of regulators’ point of view, e.g. the higher priority of long-term consequences of a source term.

All steps of the decision process were documented to allow a well-founded judgement for the end-users of this activity.
As for the review of the EURSAFE Research Issues, the following conclusions were obtained:

- The number of ERI increased to 25 up from the 21 considered in the previous work. A new ERI was added and three previous ERI were split.
- This new added ERI is also related with this report: Thermal Database and deals with the improvement of the thermo-dynamic and thermo-physical database for corium and fission products. The priority assigned to this ERI was “medium”.
- The ERIs “MCCI: molten pool configuration and concrete ablation” and “ex-vessel corium coolability, top flooding” retain their previous safety classification: high.
- The available data related to Fukushima was quite limited during the elaboration of this reassessment. Thus, only four issues derived from Fukushima were proposed for further consideration. These four issues are:
  - Pool scrubbing under boiling conditions.
  - Effect of impurities in water.
  - Spent fuel scenarios.
  - MCCI aerosol effect on chemistry.
2. Molten core-concrete interaction and debris coolability

2.1. Overview

The overall objective of this section is to review and assess principal findings from experimental programmes as well as a reactor accident that have provided information on molten corium concrete interaction under both wet and dry cavity conditions. The general goals of the experiments have been to: i) identify and characterise important phenomenological processes in order to facilitate model development, and ii) provide experimental data to support validation of models and codes that are used in reactor safety assessments. For dry cavity conditions, the research has focused on evaluating the nature and extent of the MCCI and concurrent fission product release. Conversely, under wet cavity conditions the studies have principally focused on evaluating the effectiveness of coolant in terminating the MCCI by quenching the molten core material and rendering it permanently coolable.

For dry cavity conditions, decay heat is continually dissipated by erosion of underlying concrete and can eventually lead to containment failure by axial erosion through the extent of the concrete basemat. Conversely, for BWR containments, radial erosion can undermine essential support structures such as the reactor support pedestal. Aside from exacerbating fission product release, continued ablation can lead to containment pressurisation by production of non-condensable gases arising from concrete decomposition. In addition, for scenarios in which the core debris contains significant amounts of unoxidised cladding and/or structural steel, then generation of flammable gases (H2 and CO) from the interaction of the decomposition gases (H2O and CO2) with metals present in the melt can also present a containment challenge.

Cavity flooding offers the opportunity to quench the core debris and prevent basemat melt through, greatly attenuate fission product release from core debris, and terminate containment pressurisation by non-condensable gas production. If core material that has relocated ex-vessel can be quenched and rendered permanently coolable by formation of sufficient porosity within the debris for water to ingress, then one significant aspect of the accident progression could be successfully halted. However, steam will continue to be generated as decay heat is removed from the debris, and so complete termination of the ex-vessel accident progression will inherently hinge upon: i) maintaining adequate containment cooling, and ii) ensuring that sufficient heat can be extracted from the lower part of the melt to prevent further basemat penetration. Furthermore, the distribution of debris found within the TMI-2 vessel indicates that in case of vessel failure fuel will be not always dispersed into the cavity at the same time during an accident. The quenching of relocated fuel released from a failed vessel does not preclude continued degradation and fission product release from fuel remaining in the core. These practical matters need to be factored into any evaluation of ex-vessel accident progression involving cavity flooding.

The various accident sequences and the possibility of operator intervention result in a broad range of possible initial conditions at time of vessel failure. Additionally, the state of knowledge about late in-vessel melt progression is incomplete (particularly for BWRs). Thus, there is considerable uncertainty regarding the MCCI initial conditions that include the timing of RPV failure; the initial temperature, mass, and composition of the core debris; the possibility of segregation of metal and oxide melt phases; the pour rate of the melt from the RPV that is determined principally by the melting rate of residual core material, and to a lesser extent by the opening in the RPV lower head; and finally the timing of water injection (if any). Many of these parameters (e.g. power level in the core debris, which is indicative of the time of vessel failure, as well as melt mass and composition) have been addressed in various experimental programmes that are described below.
2.2. Database summary

This section provides a summary of the database that has been generated over the last 40 years in the areas of MCCI and debris coolability. Additional technical details regarding various experimental programmes as well as the Chernobyl 4 reactor accident are provided in subsequent sections. Key data available from integral experiments and Chernobyl for code assessment under dry cavity conditions are summarised in Table 2.2-1, while flooded cavity data are summarised in Table 2.2-2. A summary of simulant material and separate effect experiments that focused on providing phenomenological insights and model development data is provided in Table 2.2-3.

Table 2.2-1: Dry cavity MCCI and aerosol generation integral experiments and accidents

<table>
<thead>
<tr>
<th>Program and test(s)</th>
<th>Description</th>
<th>Phenomena tested or characterised</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>SNL</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>BURN-1, Large Scale Trans. Tests, TURC-1T, TSS</td>
<td>Early transient tests with molten stainless steel in concrete crucibles</td>
<td>Identification of basic phenomenology associated with core-concrete interactions for different concrete types</td>
</tr>
<tr>
<td><strong>BETA</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Series I: V1.1-V1.9; V2.1-V2.3; V3.1-V3.3</td>
<td>Iron and Al₂O₃ tests in 2D cavities with induction heating to simulate decay heat</td>
<td>Influence of power level on cavity erosion behaviour and aerosol release with SIL and LCS concretes</td>
</tr>
<tr>
<td>Series II: V5.1-V5.3; V6.1-V6.2</td>
<td>Steel, Zr, and concrete oxide tests in 2D cavities with induction heating to simulate decay heat</td>
<td>Influence of Zr cladding on cavity erosion and aerosol release with SIL and serpentinite concretes</td>
</tr>
<tr>
<td><strong>SURC</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>QT-D,E; SURC-3, 3A, 4</td>
<td>Stainless steel tests in 1D cavities with induction heating to simulate decay heat</td>
<td>Influence of Zr cladding on cavity erosion behaviour and aerosol release for SIL and LCS concretes</td>
</tr>
<tr>
<td><strong>FRAG</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1, 2a, 3, 4</td>
<td>Sustained heating of steel spheres over concrete surfaces</td>
<td>Ablation rate of fragmented core debris interacting with different types of concrete</td>
</tr>
<tr>
<td><strong>HSS</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1, 2</td>
<td>Sustained heating of solid slugs of steel and UO₂-ZrO₂ over concrete surfaces</td>
<td>Ablation rate of solid debris interacting with concrete</td>
</tr>
<tr>
<td><strong>COMET</strong></td>
<td></td>
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</tr>
<tr>
<td>L2</td>
<td>Iron and Al₂O₃ in 2D concrete cavities with induction heating to simulate decay heat</td>
<td>Cavity erosion and gas release for SIL concrete</td>
</tr>
<tr>
<td><strong>MOCKA</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1,1-1.7</td>
<td>Iron plus Al₂O₃, CaO in 2D concrete cavities; chemical heating tests to simulate decay heat</td>
<td>Cavity erosion and gas release for SIL concrete</td>
</tr>
<tr>
<td><strong>HECLA</strong></td>
<td></td>
<td></td>
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<tr>
<td>1-5</td>
<td>Transient stainless steel tests in 2D cavities investigating initial transient cavity erosion phase.</td>
<td>Cavity erosion for FeSi (hematite) and ordinary SIL concrete.</td>
</tr>
<tr>
<td><strong>ACE/MCCI</strong></td>
<td></td>
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<tr>
<td>L1-L2; L4-L8</td>
<td>Core oxide tests in 1D cavities with direct electrical heating (DEH) to simulate decay heat</td>
<td>Cavity erosion and fission product release for PWR and BWR melts with SIL, LCS, LL, and serpentine concretes</td>
</tr>
<tr>
<td><strong>TURC</strong></td>
<td></td>
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</tr>
<tr>
<td>2-3</td>
<td>Transient core oxide tests in 1D crucibles</td>
<td>Initial transient erosion phase and aerosol release</td>
</tr>
<tr>
<td><strong>SURC</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1-2</td>
<td>Core oxide tests in 1D cavities with induction heating of susceptor plates to simulate decay heat</td>
<td>Effect of Zr cladding on cavity erosion and aerosol release from PWR melt composition interacting with LCS and Basalt concretes</td>
</tr>
<tr>
<td><strong>SICOPS</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core oxide tests in 2D cavities with induction heating to simulate decay heat</td>
<td>Simultaneous interaction of corium with concrete and refractory material</td>
<td></td>
</tr>
<tr>
<td><strong>VULCANO</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>B- U4-U7, VB-ES U1-U4, BS U1-U4</td>
<td>Core oxide-stainless steel tests in 2D cavities; induction heating to simulate decay heat</td>
<td>Cavity erosion for siliceous, LCS, and hematite concretes; separate effect tests with special conditions</td>
</tr>
<tr>
<td>Program and test(s)</td>
<td>Description</td>
<td>Phenomena tested or characterised</td>
</tr>
<tr>
<td>---------------------</td>
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<td>----------------------------------</td>
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<tr>
<td>NEA-MCCI</td>
<td></td>
<td></td>
</tr>
<tr>
<td>CCI 1-5</td>
<td>Large scale, core oxide, prototypic power density tests in 2D cavities; DEH to simulate decay heat</td>
<td>Cavity erosion during initial test phase for LCS and SIL concretes</td>
</tr>
<tr>
<td>Chernobyl</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Unit 4</td>
<td>RBMK-1000 reactor accident involving core melt formation within the reactor cavity and subsequent core-concrete interaction</td>
<td>Erosion of basaltic concrete within the reactor cavity over the course of days</td>
</tr>
</tbody>
</table>

Table 2.2-2: Flooded cavity debris coolability integral experiments

<table>
<thead>
<tr>
<th>Program and test(s)</th>
<th>Description</th>
<th>Phenomena tested</th>
</tr>
</thead>
<tbody>
<tr>
<td>SWISS</td>
<td>Stainless steel tests in 1D concrete cavities with induction heating to simulate decay heat</td>
<td>Cavity erosion, debris cooling rate, and aerosol scrubbing with melt interacting with LCS concrete</td>
</tr>
<tr>
<td>WETCOR</td>
<td>Oxide simulant in 1D concrete cavity with induction heating to simulate decay heat</td>
<td>Cavity erosion, debris cooling rate, and aerosol scrubbing with melt interacting with LCS concrete</td>
</tr>
<tr>
<td>COMET</td>
<td>Iron and Al₂O₃ in 2D concrete cavities with induction heating to simulate decay heat</td>
<td>Cavity erosion and debris cooling rate for siliceous concrete</td>
</tr>
<tr>
<td>ECOKATS</td>
<td>Large scale transient spreading test with oxide simulant; flooded following spreading phase</td>
<td>Cavity erosion and debris cooling rate for siliceous concrete</td>
</tr>
<tr>
<td>COTELS</td>
<td>High power density core oxide tests in 2D cavities with induction heating to simulate decay heat</td>
<td>Cavity erosion and the extent of debris quenching by the mechanisms of particle bed formation and water ingression into fragmented debris</td>
</tr>
<tr>
<td>MACE and NEA-MCCI</td>
<td>Large scale, core oxide, prototypic power density tests in 1D and 2D cavities; DEH to simulate decay heat</td>
<td>Cavity erosion and debris cooling rate for LCS and siliceous concretes; quantification of extent of cooling by melt eruption and water ingression mechanisms</td>
</tr>
</tbody>
</table>

Table 2.2-3: Simulant/separate effect experiments on MCCI behaviour, properties, and debris coolability

<table>
<thead>
<tr>
<th>Program(s)</th>
<th>Description</th>
<th>Phenomena tested</th>
</tr>
</thead>
<tbody>
<tr>
<td>ARTEMIS</td>
<td>1D and 2D dry experiments using LiCl-BaCl₂ eutectic to simulate concrete and pure BaCl₂ (as well as concrete slag for two tests) to simulate melt. Emersion heaters to simulate decay heat; gas injection to simulate concrete decomposition gases</td>
<td>Cavity erosion and crusting behaviour at melt-concrete interface</td>
</tr>
<tr>
<td>ECLAIR, CLARA, BALI</td>
<td>Controlled sparging of heated homogeneous pools with varying viscosity</td>
<td>Effect of sparging on heat flux distribution to bottom and side surfaces of pool</td>
</tr>
<tr>
<td>EPSTEIN, CASAS and CORRADINI, BALISE</td>
<td>Controlled sparging of pools with two immiscible fluids</td>
<td>Onset of mixing between stratified layers</td>
</tr>
<tr>
<td>WERLE, GREENE, ABI</td>
<td>Controlled sparging of heated pools with two immiscible fluids</td>
<td>Heat transfer coefficient between two stratified layers</td>
</tr>
<tr>
<td>PERCOLA and U. WISCONSIN</td>
<td>Controlled sparging of pools (varying viscosity for PERCOLA, 1-300 mPa·sec; water for UW tests). Upper surface covered with a special plate with penetrations that mocks up a porous floating crust.</td>
<td>Melt eruption cooling mechanism (qualitative and quantitative information on melt entrainment)</td>
</tr>
<tr>
<td>UCSB</td>
<td>Glycerine – liquid N₂ tests with controlled gas injection to simulate concrete decomposition gases (15 cm to 120 cm test section spans)</td>
<td>Debris cooling characteristics and morphology as a function of test section size</td>
</tr>
<tr>
<td>NEA-MCCI SSWICS</td>
<td>Transient reactor material corium quenching tests in 1D inert crucibles</td>
<td>Water ingress cooling and the mechanical strength of quenched corium ingots</td>
</tr>
<tr>
<td>ROCHE</td>
<td>Differential thermal analysis (DTA) and viscometer experiments with core-concrete mixtures containing</td>
<td>Liquidus-solidus and viscosity of core oxide-concrete mixtures</td>
</tr>
</tbody>
</table>
Early transient experiments with steel melts were initiated in the mid-1970s at Sandia National Laboratories (SNL) (Powers, Dahlgren, Muir, & Murfin, 1978), (Powers & Arellano, 1982-1) and in Germany (Alsmeyer, et al., 1977) to identify basic phenomenology associated with core-concrete interactions. The SNL transient testing programme continued into the 1980s using better-instrumented systems (Powers & Arellano, 1982-2), (Gronager, Suo-Antilla, Bradley, & Brockmann, 1986) as well as the incorporation of fission product mockups in the melt to assess fission product release during the early stages of core-concrete interaction. The experiment capability was expanded again as part of the Sustained Urania Concrete (SURC) programme to incorporate sustained heating of steel melts under dry cavity conditions to further characterise phenomenology associated with long-term core-concrete interaction. In particular, the SURC-3 (Copus, Blose, Brockmann, Gomez, & Lucero, 1990) and SURC-4 (Copus, Blose, Brockmann, Gopmex, & Lucero, Core-Concrete Interactions using Molten Steel with Zirconium on a basaltic Basemat: The SURC-4 Experiment, 1989) tests evaluated the effect of unoxidised cladding on the progression of core-concrete interaction for several concrete types.

The sustained heating programme at SNL also included experiments carried out with solid material to examine situations in which core debris may be quenched during relocation through water following vessel failure, and then reheat to begin core-concrete interaction. These tests were carried out as part of the HSS (Copus & Bradley, 1986) and FRAG (Tarbell, Bradley, Blose, Ross, & Gilbert, 1987) programs that examined the penetration of steel shot and slugs into different types of concrete under sustained heating conditions.

In parallel with these efforts, an extensive experimental programme involving sustained heating of metallic melts was initiated at Karlsruhe Institute of Technology (KIT) in the BETA (Alsmeyer, 1987) (Alsmeyer, et al., 1995) and COMET (Miassoedov A., et al., 2008) test facilities in Germany. These experiments provided valuable data on core-concrete interaction at a variety of power levels including gas and aerosol releases during core-concrete interaction. Recently, as part of the HECLA Program (Sevón, et al., 2010), transient metal tests have been conducted at VTT in Finland that focused on quantifying the ablation characteristics for hematite concrete that is used as the sacrificial material in the reactor pit of the European Pressurised Reactor (EPR). In addition, the MOCKA test series (Foit, Cron, Fluhrer, Miassoedov, & Wenz, 2012) was carried out to study the interaction of a simulant oxide and metal melt in a stratified configuration. To allow for a longer-term interaction without the use of an external power supply, additional energy was added to the system by alternating additions of thermit and Zr metal. These tests provide data on the heat flux distributions in the lateral and axial directions during core-concrete interaction.

In terms of reactor material testing, a series of 1D experiments (ACE/MCCI) addressing thermal-hydraulic behaviour and fission product release were carried out at Argonne National Laboratory (ANL) (Thompson D. H., Fink, Armstrong, Spencer, & Sehgal, 1992), (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995). Large scale 1D core melt tests (SURC) were also completed at SNL under transient (Gronager, Suo-Anttila, & Brockmann, 1986) as well as sustained heating (Copus, Blose, Brockmann, Simpson, & Lucero, 1992-1), (Copus, Blose, Brockmann, Simpson, & Lucero, 1992-2) conditions that looked at erosion rates as well as aerosol generation. Two-dimensional core-concrete interaction experiments (Journeau C., et al., 2009), (Journeau, et al., 2012) under dry cavity conditions have also been performed at the VULCANO test facility at Commissariat à l'énergie atomique et aux énergies alternatives (CEA) in France. These tests examined multi-dimensional cavity erosion behaviour, including the important parametric effect of high metal fraction in the core melt.
Core oxide and oxide-metal material interaction experiments in 1D and 2D cavities made of concrete as well as refractory zirconia have been carried out in the Areva SICOPS facility (Langrock & Hellmann, 2010). Finally, a series of large scale 2D core-concrete interaction experiments were conducted as part of the internationally-sponsored NEA-MCCI Program at ANL (Farmer, et al., A Summary of Findings from the Melt Coolability and Concrete Interaction (MCCI) Program, 2007), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010). These tests examined dry cavity erosion characteristics for several hours before the cavity was flooded to provide additional data on debris coolability.

Finally, during the April 1986 severe accident at Chernobyl unit 4, tens of tonnes of molten corium interacted with the reactor room concrete. Thus, this accident provided unique data on the nature and extent of core melt interaction with silica-rich (i.e. basaltic) concrete over several days under dry cavity conditions (Bogatov, Borovoi, et al., 2007), (Pazukhin 1994).

The dry cavity test results show that core-concrete interaction during the early phase is influenced by the extent of unoxidised cladding that is initially present in the melt. However, the remaining cladding is rapidly oxidised within approximately the first 30 minutes of the interaction. During the long-term, the nature of the core-concrete interaction is found to be a strong function of concrete type. In particular, for silica-rich siliceous concretes the radial/axial ablation is found to be strongly skewed radially, at least in the initial portion of the interaction when crusting on the concrete surfaces can influence the power distribution. Conversely, for limestone-rich concretes, the radial/axial ablation rates appear to be similar. For melts containing a significant fraction of unoxidised steel, stratification of the steel and oxide phases is assumed after the initial transient phase has passed. Under these conditions, the axial/radial power split appears to increase relative to tests conducted with little metal present, but the data are too sparse to draw quantitative conclusions at this point. Refractory zirconia has also been found to be effectively inert when in contact with core melt and concrete.

Fission product release has also been investigated. A range of parameters was addressed in one study (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995) by a series of tests that used four types of concrete (siliceous, limestone/sand, serpentine, and limestone) and a range of cladding oxidations for both BWR and PWR core debris. The released aerosols contained mainly constituents of the concrete. In the tests with metal and limestone/sand or siliceous concrete, silicon compounds comprised 50% or more of the aerosol mass. Releases of uranium and low-volatility fission-product elements were small. Releases of tellurium and neutron absorber materials (i.e. silver, indium, and boron from boron carbide) were high.

For flooded cavity conditions, research has principally focused on determining the effectiveness of water in terminating an MCCI by flooding the interaction from above, thereby quenching the molten core debris and rendering it permanently coolable (Table 2.2-2). As a part of this work both simulat and reactor material integral experiments have been completed to provide a database to support model development and code validation activities. High temperature steel and oxide simulant experiments were conducted at SNL as part of the SWISS (Blose, Gronager, Suo-Anttila, & Brockman, 1987) and WETCOR (Blose, et al., 1993) programs to investigate coolability with concurrent concrete erosion. Aerosol scrubbing due to overlying water was also measured as part of these tests. Large scale steel and oxide simulant core debris cooling tests have also been conducted at KIT as part of the COMET (Miassoedov, Alsmeyer, Cron, & Foit, 2010) and ECOKATS (Alsmeyer, et al., 2005) programs. In terms of reactor material testing, the COTELS (Nagasaka, et al., 1999), (Nagasaka, et al., 1999), (Zhdanov, et al., 1999), MACE (Farmer, Kilsdonk, & Aeschlimann, 2009), and NEA MCCI Programs (Farmer, et al., A Summary of Findings from the Melt Coolability and Concrete Interaction (MCCI) Program, 2007), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) have been completed to investigate the mechanisms of coolability under prototypic MCCI conditions. Depending upon melt composition and conditions, the MACE and NEA MCCI tests have revealed four mechanisms that can contribute to
core debris quenching: i) bulk cooling in which gas sparging is initially sufficient to preclude stable crust formation at the melt/water interface (and therefore, efficient heat transfer is achieved); ii) water ingress through fissures in the core material that augments what otherwise would be a conduction-limited cooling process; iii) melt (or volcanic) eruptions that lead to a highly porous overlying particle bed that is readily coolable, and iv) transient breach of crusts that form during the quench process, leading to water infiltration below the crust with concurrent increase in the debris cooling rate. In addition to information on the core debris quenching mechanisms, some tests have provided valuable data on the beneficial impact of water in scrubbing aerosols generated during core-concrete interaction.

Although it is beyond the purview of this work to perform a detailed review of non-integral experiments, it is important to note that a variety of simulant as well as reactor material separate effect tests have been conducted to identify basic phenomenology associated with core-concrete interaction and debris coolability, as well as providing data to support model development activities. Significant parts of the MCCI code models are correlations derived from these simulant experiments. These experiments are summarised in Table 2.2-3. The ARTEMIS test programme (Veteau, 2006) was carried out at CEA-Grenoble to provide data on core-concrete interaction behaviour in 1D and 2D configurations using a LiCl-BaCl2 eutectic mixture to simulate concrete and pure BaCl2 to simulate core melt. Emersion heaters were included to simulate decay heat; gas was injected over the various ablating surfaces to mock up concrete decomposition gases. These tests provided valuable data on cavity erosion behaviour including the effects of crust formation and failure at the melt-concrete interface.

As noted earlier, anisotropic ablation behaviour has been observed in reactor material experiments for different concrete types (Journeau, et al., 2012), (Farmer, et al., A Summary of Findings from the Melt Coolability and Concrete Interaction (MCCI) Program, 2007). Relevant information in this area has been provided by the ÉCLAIR (Journeau & Haquet, 2009), BALI (Bonnet, 2000) and CLARA (Bottin, et al., 2016) experiments that have focused on providing quantitative data on the heat transfer rates to horizontal and vertical surfaces in heated pools of varying viscosity that are sparged with non-condensable gas. Recent CLARA experiments (Bottin, et al., 2016) provided additional data in this area using a large scale apparatus. The data (Journeau & Haquet, 2009), (Bottin, et al., 2016) indicates that in the absence of gas sparging, natural convection heat transfer leads to a large asymmetry in the pool heat flux distribution wherein the heat transfer to the sidewalls is dominant. Conversely, for low-viscosity pools, even a modest gas sparging rate (i.e. <1 mm/sec superficial gas velocity) yields an isotropic distribution. The results of the CLARA experiments thus indicate that asymmetric gas sparging alone is not responsible for the physical differences in ablation behaviour observed for different concrete types.

In most LWR severe accident scenarios the core melt is expected to consist of discrete metal and oxide phases and these phases are not miscible. Due to the density difference between these two phases, stratification between these two phases may occur, and this can influence the heat transfer partitioning during core-concrete interaction. On this basis, experiments have been performed by Epstein (Epstein M., Petrie, Linehan, Lambert, & Cho, 1981), Casas and Corradini (Casas & Corradini, 1992), and Tourniaire and Bonnet (Tourniaire & Bonnet, 2003) in the BALISE facility at CEA in order to characterise the threshold pool sparging rate for onset of mixing between two immiscible, isothermal fluid layers. Aside from sparging rate, the thickness, viscosity, and density of the layers were varied in these experiments. One key finding is that the gas velocity required for onset of mixing between the layers increases as the density ratio of the two fluids increases. Aside from these isothermal tests, heated pool experiments were carried out by Werle (Werle, 1982), Greene (Greene, 1992), and in the ABI facility at CEA-Grenoble (Cranga, et al., 2013), (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014) in order to characterise the interfacial heat transfer coefficient between the two layers in a stratified state. These
experiments have shown that the interfacial heat transfer coefficient between two layers is generally quite large when the pool is sparged, and that the presence of a metal layer can change the heat transfer characteristics relative to that observed for other simulant fluids (e.g. water, oil, Freon).

Aside from the above mentioned programmes that address generic phenomenological issues related to core-concrete interactions, other simulant and separate effects tests have been conducted to provide data in the area of debris coolability. The PERCOLA experimental programme was carried out at CEA-Grenoble by Tourniaire et al. (Tourniaire B., Seiler, Bonnet, & Amblard, 2000) to provide fundamental insights into the melt eruption process under well-controlled experiment conditions. These tests utilised fluids with different viscosities to simulate the enrichment of corium with silica (SiO2) during core-concrete interaction. The experiments revealed liquid ejection by single-phase extrusion (or ‘fountain’) and two phase jetting mechanisms through a floating solid layer that represented the interfacial crust that forms during core-concrete interaction. Robb and Corradini (Robb & Corradini, September 2011) developed a facility for melt eruption experiments that took into account the experience gained during the PERCOLA programme (Tourniaire B., Seiler, Bonnet, & Amblard, 2000). These experiments expanded the PERCOLA database leading to the development of several entrainment models (Tourniaire B., Seiler, Bonnet, & Amblard, 2006) including a phenomenological entrainment model (Robb & Corradini, September 2011).

To identify phenomena associated with melt coolability, (Theofanous, Liu, & Yuen, 1998) conducted coolability tests using glycerine and liquid nitrogen to simulate the melt and overlying coolant, respectively. Gas sparging from MCCI was simulated by sparging air through a porous plate located at the bottom of the apparatus; decay heat was simulated by the sensible heat deposited in the glycerine due to cooling of the sparged gas. For small-scale tests, quench was not achieved due to the formation of wall-anchored crusts that inhibited the melt-coolant interaction. However, for the larger tests, quench was generally achieved, and the time to quench increased with initial melt depth and decreased with gas sparging rate. The principal mechanism leading to coolability in the quenched tests was volcanic eruptions, leading to enhanced debris surface area available for contact with the overlying coolant.

Aside from the simulant material experiments, the SSWICS reactor material separate effect experiments were conducted as part of the NEA MCCI Program (Farmer, et al., A Summary of Findings from the Melt Coolability and Concrete Interaction (MCCI) Program, 2007), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) to provide data on: i) the water ingression cooling mechanism, and ii) the mechanical strength of crust material formed during the quench process. These experiments provided the technical basis for the development of a correlation for the effective crust dry-out limit due to ingestion that can be used in debris coolability analyses (Lomperski, Farmer, & Basu, 2006), (Lomperski & Farmer, 2007). The crust strength data is valuable in terms of assessing the potential for crust anchoring to occur in plant accident scenarios (Lomperski & Farmer, 2009), since anchoring and subsequent melt-crust separation due to ongoing concrete ablation can effectively terminate quenching processes.

In addition to separate effect experiments focused on debris coolability, several experiments have been conducted to measure additional material and transport properties for ex-vessel conditions. In particular, (Roche, Steidl, Leibowitz, Fink, & Sehgal, 1993) conducted early experiments to evaluate liquidus-solidus temperatures as well as viscosity for mixtures of core oxides (UO2, ZrO2) and concrete for various concrete types. The PRECOS experimental programme (Bottomley, et al., 2012) was subsequently conducted to evaluate phase diagrams for mixtures of UO2–SiO2, UO2–CaO, UO2–FeO–SiO2, and UO2–FeO–CaO. Recent tests in the COLIMA facility (Journeau, 2005) have been carried out to investigate aerosol generation over MCCI pools, as well as aerosol retention in physical structures such as cracks in concrete.

Finally, although the current database assessment is focused on core-concrete interaction and debris coolability under top flooding conditions, it is further noteworthy that several experimental
programmes have been conducted to evaluate the effectiveness of proposed core-catcher concepts for Gen 3+ LWR plants. For ex-vessel conditions, two general methods have been explored for achieving long-term melt stabilisation:

A crucible-type technique in which sacrificial material is first introduced into the melt through ablation at the melt/crucible boundary (with the additives acting to lower the melt pool freezing point), and then cooling of the crucible exterior boundary with water.

Melt fragmentation, in which water is introduced at the bottom of the melt pool at a slight overpressure, and the ensuing steam formation acts to cool and solidify the melt in a highly porous configuration that is readily permeable by water.

An example of the crucible technique is provided by the EPRTM (Fischer & A. Henning, 2009) that features a spreading room adjacent to the reactor pit in which the melt is cooled by water from the bottom and top after spreading is completed. Various experiments have been conducted to demonstrate the heat removal capabilities of this system (Fischer, Herbst, & Schmidt, 2005), including a generically designed large-scale reactor material experiment that illustrated the capability of a water-cooled plate in stabilising a core melt after ablating through a layer of sacrificial concrete (Farmer, Lomperski, Kilsdonk, & Aschlimann, 2010).

The melt fragmentation technique, denoted COMET, was pioneered by Forschungszentrum Karlsruhe (FZK). Both simulant and reactor material tests have shown that there are very effective means for quenching and stabilising core melt (Alsmeyer, Farmer, Ferderer, Spencer, & Tromm, 1998), (Journeau & Alsmeyer, 2006). However, this concept has not yet been deployed in any reactor design.

2.3. Dry cavity MCCI experiments

The previous sections have outlined phenomenology and supporting experiments related to core-concrete interaction under dry cavity conditions. This section provides additional technical details from selected experimental programmes regarding core-concrete interaction under dry cavity conditions. The Chernobyl Unit 4 accident also provided valuable post-mortem information in this area; details are provided in Section 2.5.

2.3.1. BETA experiment series

The BETA facility was constructed and operated by KIT (formerly Kernforschungszentrum Karlsruhe, or KfK) in Germany as a large scale test facility to study melt concrete interaction in a cylindrical concrete crucible (Figure 2.3-1) (Alsmeyer, 1987), (Alsmeyer, et al., 1995). Two test series were conducted. In the BETA Series I experiments, the melt (1) typically consists of 300 kg steel (Fe,Cr,Ni) and 150 kg of simulant oxide (initially Al2O3, CaO, SiO2). The melt was generated by a thermite reaction in the reaction tank (5) and poured into the concrete crucible (2) with an initial cavity of ID 38 cm. The initial melt temperature was ~ 2000°C.
Sustained heating was applied through a cylindrical induction coil (3) surrounding the crucible; the power was deposited in the metallic phase. Instrumentation was provided to detect the location of the ablation front, temperature and material sampling of the melt, off-gas composition and flow rate, aerosol density and composition in the off-gas, and net input power to the melt. Video footage of the melt upper surface behaviour was also obtained. In the Series I experiments, fission product simulants were not placed in the melt. Table 2.3-1 summarises the 19 experiments that were carried out in the Phase I test series (1984 to 1986). Most of the experiments used siliceous concrete, but three used limestone or limestone/common sand concrete. Variations in input power were intended to simulate different phases of the interaction; i.e. high power densities simulated the early high heat flux and temperatures shortly after RPV failure, while low power input corresponded to the long term decay heat level and quasi-steady melt characteristics after the transient cool-down phase.

For these tests, the interaction was dominated by the metallic melt due to the induction heating technique that deposited energy only in that phase. In reactor situation, only the residual power is mainly dissipated in the oxidic phase. The consequence is that the overlying oxide melt was cooler and possibly below the melting temperature. Thus, concrete erosion by the oxide was limited and probably overestimated by the metal. However, an advantage of induction heating is that interstitial heating elements (e.g. electrodes) were not required, thereby allowing a true cylindrical cavity configuration to be simulated. Additionally as the molten concrete and the metallic melt are not miscible, the volume of the metallic heated layer does not increase with the concrete ablation and so the heat flux does not decrease contrary to reactor situations.
<table>
<thead>
<tr>
<th>Test</th>
<th>Melt Composition</th>
<th>Power (kW)(^1)</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>V0.1</td>
<td>Iron</td>
<td>0</td>
<td>Test of facility</td>
</tr>
<tr>
<td>V0.2</td>
<td>Iron</td>
<td>400</td>
<td></td>
</tr>
<tr>
<td>V0.3</td>
<td>Iron + Oxide</td>
<td>1 700</td>
<td></td>
</tr>
<tr>
<td>V1.1</td>
<td>Iron</td>
<td>Pulsed</td>
<td>Power failed</td>
</tr>
<tr>
<td>V1.2</td>
<td>Iron + Oxide</td>
<td>Pulsed</td>
<td>Lorentz forces excluded</td>
</tr>
<tr>
<td>V1.3</td>
<td>Steel + Oxide</td>
<td>1 000</td>
<td></td>
</tr>
<tr>
<td>V1.4</td>
<td>Steel</td>
<td>0</td>
<td>Transient</td>
</tr>
<tr>
<td>V1.5</td>
<td>Steel + Oxide</td>
<td>450</td>
<td></td>
</tr>
<tr>
<td>V1.6</td>
<td>Steel + Oxide</td>
<td>1 000</td>
<td></td>
</tr>
<tr>
<td>V1.7</td>
<td>Steel + Oxide</td>
<td>1 700</td>
<td></td>
</tr>
<tr>
<td>V1.8</td>
<td>Steel + Oxide</td>
<td>1 900</td>
<td>No dispersion (CaO)</td>
</tr>
<tr>
<td>V1.9</td>
<td>Steel + Oxide</td>
<td>400 – 200</td>
<td>CaO added</td>
</tr>
<tr>
<td>V2.1</td>
<td>Steel + Oxide</td>
<td>120–150</td>
<td></td>
</tr>
<tr>
<td>V2.2</td>
<td>Steel + Oxide</td>
<td>50–90</td>
<td>CaO added</td>
</tr>
<tr>
<td>V2.3</td>
<td>Steel + Oxide</td>
<td>240</td>
<td>CaO added</td>
</tr>
<tr>
<td>V3.1</td>
<td>Steel + Oxide</td>
<td>1 700 – 2 500</td>
<td>US Limestone/Quartz sand, heating from 0 to 66 s only</td>
</tr>
<tr>
<td>V3.2</td>
<td>Steel + Oxide</td>
<td>400 – 1 000</td>
<td>US Limestone, 30 min heating</td>
</tr>
<tr>
<td>V3.3</td>
<td>Steel + Oxide</td>
<td>600 – 200</td>
<td>US Limestone/Quartz sand, 60 min heating</td>
</tr>
<tr>
<td>V4.1</td>
<td>Steel + Oxide</td>
<td>1 000 – 300</td>
<td>600 mm dia. crucible</td>
</tr>
</tbody>
</table>

The oxide phase differs from prototypic melt due to the lack of UO2-ZrO2. Thus, the oxide initially has a lower density but in the long term both melts are expected to have more similar physical properties due to the addition of slag from the decomposing concrete. The result is that the BETA experiments favour segregation of metal and oxide phases with the metal at the bottom. In the BETA tests the oxide temperature is expected to be lower than the prototype because of the lack of internal heating in that phase.

The metallic melt composition used in the Series I experiments provides a good representation of the accident composition for structural steel, but there is a lack of unoxidised Zircaloy cladding which is important in the early interaction phase. However, the role of Zr was investigated in the Series II tests described below.

The experimental results for siliceous concrete are summarised as follows. For melts with high temperatures and high input power input, characterised by small influence of crusts at the melt/concrete interface, propagation of the metallic melt was predominantly downward. This trend was especially pronounced in Test VI.8; the post-test cavity profile is shown in Figure 2.3-2. This test simulated the early high temperature interaction including exothermal chemical interaction processes from with an overall internal heating rate equivalent to 4.5 kW/kg. This power level exceeds the decay heat level for a typical PWR at 2 hours following scram by a factor of ~20. The interaction was

1. For a 300 kg mass of steel and a 150 kg of simulant oxide the power density range from 111 W/kg up to 5 555 W/kg, whereas a typical power density for a PWR at 2 hours following scram is about 200 W/kg.
dominated by violent gas release that agitated the melt, depositing material on the upper crucible walls. Over the eight minute heating period the melt eroded ~50 cm of the crucible axially at a nearly constant erosion rate of 1 mm/s. The downward heat transfer was so effective that the melt cooled down, despite the high heating rate, close to the solidification temperature (Figure 2.3-3).

The evolved gases from concrete decomposition (predominately H₂O for siliceous concrete) resulted in a substantial release of H₂ from the reduction of steam by the metal. The molar ratio of H₂ -H₂O to CO-CO₂ was characteristic of silicate concrete.

Figure 2.3-2: Section through the test crucible for BETA test V 1.8

Figure 2.3-3: Temperature of the melt pool for BETA test V 1.8

The low power BETA experiments illustrated the role of solidification processes (viz. crust formation) on cavity erosion behaviour. The bottom crust of the metal melt at the lower metal/concrete
interface reduced the extreme downward heat transfer and a more balanced downward-sideward concrete erosion pattern was observed. The crusts of the partly solidified melt were permeable to gases released from the concrete, and these gases continued to agitate the melt, thus maintaining convection which in the long term reduced the melt temperature to near the solidification point. The typical long term erosion velocity was of the order of a few centimetres per hour. During this phase, H₂ continues to be produced as long as any metal was present in the melt.

The Series I tests also illustrated the tendency for aerosol release in the absence of fission products and with no core oxides present in the melt. Condensation aerosols were observed, generally in the form of agglomerated particles of a few µm in diameter. The primary aerosol particles had a submicron diameter typical for condensation aerosols. All experiments showed intense aerosol release during the first 1 or 2 minutes of the interaction that correlated with the high gas release immediately after the melt was poured into the crucible. The release rate then fell to ~ 0.1 g per mole of gas released from the concrete for both low- and high-power experiments.

The overall cavity erosion behaviour for the limestone concretes was similar to tests with siliceous concrete, with the exception that lateral erosion for the limestone tests was more pronounced relative to siliceous. The large amount of CaCO₃ in the concrete is calcined, releasing CO₂ near 700°C. The resulting CaO (burnt lime) is a very soft material and was found in the sectioned crucibles in a zone a few millimetres ahead of the melting interface. At low heat transfer rates, this process could influence the concrete decomposition process; i.e. instead of melting, the CaO could disintegrate by spallation and dissolve in the melt. This behaviour is different than siliceous concrete that contains crystalline silica (quartz) aggregate; this material remains intact and does not dissolve quickly in the melt. For these tests CO₂ dominated the gas release, and part of this gas was reduced to CO by reaction with the metallic melt. One primary difference between the two concrete types is copious aerosol production for the limestone tests. In particular, a dense white aerosol was released with a typical diameter of 1 µm. The aerosol production rate was estimated to exceed 1.2 g per mole of gas from concrete decomposition. The particles were predominately composed of CaO with traces of Na and K.

The BETA II Series was performed from 1990 to 1992 and included 6 experiments (Table 2.3-2). Experiments V5.1 to V5.3 investigated the influence of Zr (i.e. Zircaloy-4) on concrete erosion following the SURC-4 experiments that indicated the potential for Zr-SiO₂ condensed phase reactions (Copus, Blose, Brockmann, Gopmex, & Lucero, Core-Concrete Interactions using Molten Steel with Zirconium on a basaltic Basemat: The SURC-4 Experiment, 1989). In addition, B4C was added to represent BWR-specific melt composition. Experiments V6.1 and V6.2 investigated the thermal stability of a vertical concrete cylinder cooled by water on the outside as melt eroded the inner surfaces. Finally, experiment V7.1 studies the behaviour of Russian serpentinite concrete. All Series II experiments were performed in cylindrical crucibles similar to the first test series with the same induction heating technique. Modifications were made to allow the delivery of metallic Zircaloy and fission product mockups to the melt, and to improve aerosol measurements and sampling. Redundant and diverse thermocouple techniques were also provided to optimise melt temperature measurements.

For the V5 experiment series, the results indicate that extremely rapid oxidation of the Zircaloy-4 inventory during the first 2 to 3 minutes of the experiments. Material investigations showed that depletion of Zr had been initiated by 1 minute with a simultaneous increase of Si concentration in the metallic melt, as is expected to occur by the condensed phase chemical reaction $\text{Zr} + \text{SiO}_2 \rightarrow \text{ZrO}_2 + \text{Si}$. In spite of the high energy deposition from Zr oxidation and from electrical heating, the temperature of the metal in the three experiments dropped to near the freezing temperature within 150 s. This trend was combined with rapid cavity erosion, high gas release, and extreme voiding of the melt. Gas release showed a pronounced spike during the first interaction period and then fell to a lower level in the latter phase of the tests. The axial versus radial erosion behaviour depended on the long-term power input to the melt; this observation is in agreement with the BETA I experiment findings. High
aerosol release rates were observed during Zr oxidation; these fell after the Zr was depleted. In terms of fission product transport measurements, substantial Te was released, whereas the release of Ce and La were much greater than Sr and Ba. The release of Mo was small. Some portion of B4C that was added to the melt (to represent BWR absorber material) led to formation of borates in the oxide phase. These borates contributed to aerosol production.

### Table 2.3-2: BETA Series II experiments

<table>
<thead>
<tr>
<th>BETA test</th>
<th>Initial melt (~2200 K)</th>
<th>Planned heating power (kW)</th>
<th>Main objectives</th>
</tr>
</thead>
<tbody>
<tr>
<td>V5.1</td>
<td>300 kg Fe + Cr + Ni 80 kg Zry-4 50kg Al₂O₃/SiO₂/CaO</td>
<td>400</td>
<td>PWR: Zr chemistry, consequences on erosion and aerosol release</td>
</tr>
<tr>
<td>V5.2</td>
<td>Same as V5.1</td>
<td>200</td>
<td>BWR with B4C absorber, low power: Zr chemistry and related processes</td>
</tr>
<tr>
<td>V5.3</td>
<td>Same as V5.1</td>
<td>800</td>
<td>BWR with B4C absorber, high power: Zr chemistry and related processes</td>
</tr>
<tr>
<td>V6.1</td>
<td>300 kg Fe, Ni 50kg Al₂O₃/SiO₂/CaO + Zr₂O₃ + Cr₂O₃</td>
<td>120</td>
<td>Failure of concrete shield and possibility of late melt flooding</td>
</tr>
<tr>
<td>V6.2</td>
<td>Same as V6.1</td>
<td>120</td>
<td>Failure of concrete shield and possibility of late melt flooding</td>
</tr>
<tr>
<td>V7.1</td>
<td>300 kg Fe, Cr, Ni; 80 kg Zr 50kg Al₂O₃/SiO₂/CaO</td>
<td>400</td>
<td>Serpentine concrete behaviour</td>
</tr>
</tbody>
</table>

The formation of SiO was of minor importance in these experiments, and this may be explained by the relatively low temperature of the melt in comparison to the ACE experiments where SiO formation and release played an important role (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995). The low freezing temperature of the metal is explained by the addition of Zr, Si, B, Cr, and Ni to the Fe; this effect needs to be considered in computer codes.

The BETA V5.1 experiment was utilised for NEA Standard Problem ISP30 (Alsmeyer, Farmer, Ferderer, Spencer, & Tromm, 1998). The conclusion from this exercise is that modelling of condensed phase chemistry is required to describe the rapid Zr oxidation.

Experiment V7.1 conducted with serpentine concrete yielded much higher gas release, which is attributable to the high content of bound water in the serpentine mineral. Consequently, substantial H₂ release was observed. The MgO content of the decomposing concrete lowered the viscosity and solidus temperatures of the multicomponent oxide melt in comparison with pure silicate concrete. MgO contributed to aerosol production, nearly doubling the total aerosol mass.

The two experiments V6.1 and V6.2 on radial concrete penetration simulated the expected decay heat level at 8 hours after initiation of concrete erosion. The experiments clearly demonstrated that outside water cooling is unable to stabilise the melt in the concrete. This is due to the fact that the concrete thickness by which heat can be transferred through the walls by conduction is only 5 to 10 mm, and this concrete thickness is mechanically unstable. Hence, further spreading of the melt and direct contact with water will occur.

2. Power density ranging from 342 W/kg up to 1 860 W/kg
2.3.2. COMET-L2 experiment

The COMET-L2 experiment (Miassoedov A., et al., 2008) was carried out using base technology developed as part of the BETA Program (Alsmeyer, 1987), (Alsmeyer, et al., 1995) to investigate long-term melt–concrete interaction of metallic corium with siliceous concrete in a 2D cylindrical crucible. The test was carried out under dry conditions with simulation of decay heat at intermediate levels during the first phase of the experiment, and at low levels during the second phase. The apparatus is shown in Figure 2.3-4.

The corium melt is simulated by a metallic phase (~ 430 kg Fe and Ni) initially overlaid by an oxide phase (~35 kg Al2O3–CaO). The melt was produced by a thermite reaction at an initial temperature of ~1 750°C in a separate melt generator. Oxide additives were incorporated into the melt to lower the solidification temperature and increase the solidus–liquidus temperature range to be more reflective of actual core debris (e.g. see Roche, Steidl, Leibowitz, Fink, & Sehgal, 1993). The melt was then poured into the 60 cm diameter concrete test crucible and induction heating was applied from below the test section to the metal phase to simulate decay heat levels in the range of 150–250 kW. This power level is indicative of ex-vessel accident conditions a few hours after the start of the cavity erosion. In the second phase of the test, input power was reduced to ~200 kW to observe the onset of possible crust formation and its influence on the erosion process. The concrete crucible was instrumented with thermocouples to detect the location of the ablation front and to provide an indication of the melt temperature. An optical pyrometer was used to provide debris upper surface temperature measurements. The facility also included an instrumented off-gas system.

Upper and lower bound estimates on the oxide phase temperature during the experiment based on optical pyrometer measurements are provided in Figure 2.3-5. These measurements indicate a rapid initial reduction in the oxide phase temperature, in agreement with the BETA measurements (Alsmeyer, 1987), (Alsmeyer, et al., 1995). The cavity erosion data (Figure 2.3-6) indicates that the initial axial-radial erosion ratio is ~1:1 until the melt superheat is effectively lost by heat transfer to the concrete. This occurs at ~100 seconds into the test sequence. However, during the longer-term interaction, the axial erosion rate is two to three times higher than the radial rate. Heating of the melt was terminated at 1 015 seconds when the axial erosion limit for the facility was reached. As shown
in Figure 2.3-7, final axial ablation depths were in the range of 9 to 17 cm, while radial erosion was limited to ~ 3 cm.

**Figure 2.3-5:** Oxide melt temperature evolution in COMET-L2

**Figure 2.3-6:** Axial (solid lines) and radial (dotted lines) concrete erosion by the metal melt phase in COMET-L2
During the long-term interaction, quiescent periods were interrupted by short but intense periods of melt eruptions that may be attributable to formation and failure of metal crusts at the core-concrete interface. Low heat fluxes to concrete result in thermal decomposition of the concrete matrix and the release of un-melted silica aggregate into the melt. When compared to heat fluxes experienced early in the sequence, this process would lower the effective concrete decomposition enthalpy and may alter the heat flux and concrete erosion patterns in the crucible. In addition, incorporation of solid aggregate will cause bulk cooling of the melt to occur.

2.3.3. SNL dry cavity experiments

Sandia National Laboratories carried out several experiment series on core-concrete interaction to support model development and code validation activities (Table 2.2-1). Both transient as well as sustained heating tests were carried out, and the various experiment series are described under these two headings below since these topical areas roughly correlate with the chronological order under which the tests were carried out. Both simulant as well as reactor materials were used in the test programmes.

Transient experiments

Early transient experiments were carried out as part of the Large Scale test series in which molten stainless steel at ~1700°C was poured into limestone-common sand (LSL tests 1-4), basalt (LSB tests 2-4), and limestone (LSCRBR tests 1-2) hemispherical crucibles to provide basic information on melt-concrete interaction processes. Each test crucible was exposed to molten steel several times to simulate prolonged periods of interaction.

These experiments indicated that gas generation from concrete decomposition leads to a strong initial melt agitation phase for all three concrete types. As the material cools, agitation diminishes to the point where the interaction resembles a boiling liquid. A viscous layer of slag eventually forms over the metal; the layer is permeable to the sparging gases. As the material cooled, the slag eventually hardened but vent holes for gases persisted. H2 and CO generated by reduction of water vapour and carbon dioxide from metals in the melt were detected over the interaction. Aerosols from oxidised metal and concrete decomposition products were also detected. In general, these tests provided qualitative indications of phenomena that were later confirmed in sustained heating experiments.
These early experiments were followed by the TURC transient tests that featured improved instrumentation to investigate limestone-common sand (LCS) concrete erosion behaviour in 1D test crucibles. The main objective was to compare metal melt erosion behaviour in tests TURC-1T and TURC-1SS (Gronager, Suo-Antilla, Bradley, & Brockmann, 1986) to core oxide (UO2-ZrO2) behaviour in tests TURC-2 and TURC-3 (Gronager, Suo-Anttila, & Brockmann, 1986). All experiments used 41 cm ID refractory MgO crucibles lined at the bottom with a slab of LCS concrete. The melts were first produced (using a thermite reaction for the steel tests and by induction heating of embedded tungsten susceptors for the core oxide experiments) and then poured into the test crucibles. Initial melt temperatures ranged from 2100 to 2500°C. Concrete erosion was limited to ~7 cm; the erosion rate ranged from ~0.5 mm/sec at the start of the transient down to ~0.03 mm/sec near the end. Gas measurements confirmed the formation of CO and H2. Melt temperature measurements during the interaction were not obtained, and so information on heat transfer coefficients could not be obtained.

Aerosol measurements in TURC-1SS indicated concentrations in the range of 15 to 80 g/m³, which are considerably higher than the levels of ~5 g/m³ measured in the BETA tests. The difference may be explainable as follows. The melt in the TURC-1SS experiment was covered by a thin layer of oxide whereas in BETA the layer depth was significant. Second, the major release of aerosols in TURC-1SS was in the form of volatile fission product simulants (i.e. Te, I, and Cs), and these simulants were not present in the BETA tests. Low levels of Ce and La were also detected. The mean diameter of the aerosols was 1 µm or less.

The core oxide melt masses in the TURC-2 and TURC-3 experiments were 147 kg and 46 kg, respectively, with a composition reflective of a PWR (i.e. 70 wt% UO2 and 30 wt % ZrO2). In TURC-3 experiment, 9 wt % metallic Zr was added to the melt. Both experiments indicated rapid crusting of the melt upon introduction into the crucibles with no detectable concrete ablation. The small amount of superheat in these melts in conjunction with the lack of input power to simulate decay heat allowed the melts to cool without significantly damaging the concrete. Even though the cavities were not ablated, steam and CO2 were still released and these gases were partially reduced to H2 and CO. Detected aerosols consisted mostly of Te, I, and Cs. The low volatile fission products had low concentrations or were below the detection limit. Note that an initial rapid crusting phase leading to a delay in the onset of ablation under sustained heating conditions was also observed in the CCI tests carried out in the NEA MCCI Program (Farmer, et al., A Summary of Findings from the Melt Coolability and Concrete Interaction (MCCI) Program, 2007), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010).

**Sustained heating experiments**

Early sustained heating experiments were carried out as part of the HSS (Copus & Bradley, 1986) and FRAG (Tarbell, Bradley, Bloke, Ross, & Gilbert, 1987) programs to examine the penetration of solid steel and core oxide debris into different types of concrete. These tests examined scenarios in which core debris may be quenched during relocation through water following vessel failure, and then reheat to begin core-concrete interaction. This type of interaction may also occur late in the sequence after cool-down and partial freezing of the metallic phase which would reside at the bottom of the melt pool after the core oxides are diluted with slag from concrete decomposition.

The FRAG experiments used 45 kg of steel spheres (3-4 mm diameter) interacting with both basalt and LCS concrete. The steel was inductively heated for up to four hours. In two of the four tests, water was added to the debris after the interaction was initiated to determine the effect of flooding on the erosion behaviour and temperature of the debris.

In the absence of water, the results show that the solid material reaches a temperature between 1200 and 1400°C. Axial erosion rates were in the range of 3 to 4 cm/h. Slag from the erosion process migrated through the debris and formed a crust covering the upper surface. Two-thirds of the steam and CO2 from concrete decomposition were reduced to H2 and CO by oxidation of the hot steel.
spheres. The addition of water showed that a crust layer may limit water ingestion; complete quenching of the debris did not occur.

The HSS-1 and HSS-3 (Copus & Bradley, 1986) experiments produced similar results with a heated solid slug of steel (HSS-1) and a heated slug of UO2-ZrO2 (HSS-3). Erosion was maintained in these tests over periods of 3 to 4 hours at a typical rate of 1.5 cm/h; the total erosion depth was ~6 cm (Figure 2.3-8). The average steel temperature was 1 350°C, while the bulk temperature of the oxide temperature was 1 650°C; both of these temperatures are well below the melting points of the respective materials.

![Figure 2.3-8: Concrete erosion by steel (HSS-1, left) and by UO2-ZrO2 slugs (HSS-3, right) (Copus & Bradley, 1986)](image)

The FRAG and HSS tests thus showed that even with low heat fluxes imposed by hot solid material, the material is able to melt the concrete as long as heat conduction into the concrete is unable to remove the heat.

Aside from the solid material tests, concrete erosion by liquid melt with sustained heating was investigated in the SURC test series. The SURC-4 test (Copus, Blose, Brockmann, Gopmex, & Lucero, Core-Concrete Interactions using Molten Steel with Zirconium on a basaltic Basemat: The SURC-4 Experiment, 1989) was the first experiment to illustrate the role of condensed phase chemical reactions between Zr and SiO2 from concrete erosion. Major components of the SURC-4 apparatus included a sealed and water-cooled containment vessel, interaction crucible, induction coil, and zirconium delivery tube (Figure 2.3-9 SURC-4 experiment apparatus). As for the TURC experiment series, the test crucible consisted of a 40 cm ID refractory MgO cylinder with a basalt concrete slab located at the base, thus limiting erosion to the axial direction. Thermocouples were used to measure melt temperature, as well as local erosion rates and temperatures within the concrete. Additional instrumentation was provided to monitor the composition and flow rate of evolved gases. An aerosol detection system was also incorporated to measure the composition and release rates as a function of time.
The experiment was carried out with a 200 kg stainless steel charge with an additional 6 kg of simulated fission products (Mo, Te, La2O3, CeO2, BaO). The melt was produced by in-situ induction heating. At 105 minutes in the experiment sequence concrete erosion began. At 119 minutes, 20 kg of zirconium metal was dropped into the melt. The experiment proceeded until 162 minutes, corresponding to 57 minutes of concrete erosion with a net heating rate that ranged from 51 to 62 kW. At this point, the test section failed which effectively terminated the experiment. After the Zr was added, the concrete erosion near the centre of the crucible increased over several minutes from 16 cm/hr to 30 cm/hr (Figure 2.3.10). In addition, the gas volumetric flow rate (mainly H2) and aerosol production rate increased dramatically at this time, apparently driven by the Zr oxidation process. Simultaneously, an increase in melt temperature of ~130 K occurred (Figure 2.3.10), driven by the exothermic nature of the Zr condensed phase chemical reaction, in parallel with gas phase reactions. Large volumes of foam slag with 17-18% of oxidised ZrO2 also formed during this interaction. Aside from these observations, the experiment provided no indications of the coking reaction (formation of elemental carbon), as CO was continuously released. Regarding aerosol chemistry, Te was found to be the dominant species with lesser amounts of Fe, Mn, Na, and K released. For the other fission product simulants, Ce was detected at a level below 1%, while Ba, Mo, and La releases were below the detection limit (< 0.1%).

The SURC-3 and SURC-3A experiments (Copus, Blose, Brockmann, Gomez, & Lucero, 1990) were carried out to determine whether CO2 from concrete decomposition would be reduced by Zr to produce elemental carbon through the coking reaction. The crucibles in these tests had an ID of 21.6 cm, and limestone concrete was utilised which would optimise the chances for coking reactions if they were to occur. The SURC-3 experiment utilised a 1D crucible (concrete at the base), while the crucible for SURC-3A was made entirely from limestone concrete.
The experimental procedure was similar to that used in SURC-4. After the 50 kg stainless steel charge was melted and concrete erosion initiated, Zr was added to the melt, resulting in a Zr concentration of 2% in SURC-3 and 5% in SURC-3A. In both tests, the concrete ablation rate, melt temperature, off-gas rate, and aerosol concentration increased over a period of 10-15 minutes following Zr addition. In both experiments, CO was the dominant gas species both prior to and after Zr addition, which confirms that the coking reaction did not occur at a significant level. Rather, the energy source driving increases in melt temperature and concrete ablation rate was the highly exothermic reaction between Zr and H2O and CO2 from concrete decomposition, yielding H2 and CO. The typical temperature of the melt pool was 1600°C, and the pool temperatures were reported to increase by 80 to 200°C during the oxidation phase. The aerosols were comprised primarily of Te, Na, K and Fe oxides. The release of other low volatile fission products was generally below 0.02%. The SURC-3A experiment resulted in radial concrete erosion to a depth of 9 cm versus 25 cm in the axial direction. This ratio is comparable to the BETA V3.2 experiment (Alsmeyer, et al., 1995).

The SURC-1 (Copus, Blose, Brockmann, Simpson, & Lucero, 1992-1) and SURC-2 (Copus, Blose, Brockmann, Simpson, & Lucero, 1992-2) experiments were reactor material tests that provided 1D core-concrete interaction data for limestone-common sand and basalt concrete types, respectively. Both tests utilised ~200 kg of core oxide (UO2 – ZrO2 ) containing 18 kg of metallic Zr. The overall facility layout and crucible dimensions were the same as that used for SURC-4 (see Figure 2.3-9 SURC-4 experiment apparatus). Decay heat was simulated by induction heating of tungsten susceptors located directly in the core debris. The input power ranged from 50 to 100 kW, which corresponded to specific power densities of 250 to 500 W/kg. These power densities are indicative of a PWR a few hours after shutdown. In addition to the UO2/ZrO2/Zr core charge for both tests, 3.4 kg of fission product simulants were added prior to heating. These additives consisted of BaMo04, La203, Ce02, and Nb205.

The tests showed two characteristic phases after onset of concrete ablation. In the first phase when metallic zirconium was still present, the erosion rate was as high as 15-30 cm/hr (see Figure 2.3-10 and Figure 2.3-11). After the Zr was fully oxidised, the ablation rate decreased to 5-15 cm/h. Since the decomposition enthalpy of limestone concrete is higher compared to Basalt, the erosion rate in SURC-1 was lower than that compared to SURC-2. The high early erosion rate for both tests is attributable to exothermic oxidation of Zr by concrete decomposition gases. Due to the high carbonate content, the gas composition for the limestone concrete test was dominated by CO and CO2, with CO dominant during the Zr oxidation process. In contrast, the gas composition for the basalt test was dominated by H2. However, the cumulative gas release for this test was lower.
The temperatures of the melt pool (see Figure 2.3-10 and Figure 2.3-11) were initially controlled by the high melting point of the core oxide mixture, but as concrete decomposition products were added, the pool temperature decreased throughout the test to 1 700-1 800°C. In general, the temperatures of the oxidic melts are several hundred degrees higher in comparison to the metallic melt tests. Aerosol release rates ranged from 1 to 10 g/min for both tests. The aerosol compositions were dominated by concrete species (Si, Na, K, and Ca). The U and Ba concentrations were less than 0.3%. Releases of Mo, La, Ce and Nb were smaller still. Releases of some of the refractory components were smaller for the basalt test, which may be attributable to silicates from the basaltic concrete.

![Figure 2.3-11: SURC-1 (top) and SURC-2 (bottom) axial erosion front location (left) and melt temperature (right)](image)

### 2.3.4. ACE phase C experiments

The ACE Phase C experiments were carried out at Argonne National Laboratory between 1988 and 1991, see (Thompson D. H., Fink, Armstrong, Spencer, & Sehgal, 1992), (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995). The objectives of these tests were to: i) quantify fission product release during core-concrete interaction, and ii) provide data on thermal-hydraulic behaviour to support model development and code validation activities. All experiments used an initial charge mass of ~300 kg. The melt consisted of U02-Zr02, fission product simulants, concrete decomposition products, and some structural materials to obtain a typical corium mixture. Several tests included
control rod material to evaluate the effect of this material on fission product release. Decay heat was simulated using direct electrical heating (DEH) of the core debris at power levels experienced a few hours into the accident sequence.

The apparatus was 1D with an internal cross-section of 50 cm x 50 cm (see Figure 2.3-12). The concrete basemat was situated at the base of the apparatus. In most tests zirconium-bearing concrete/metal inserts were placed immediately over the basemat that contained the initial metal content of the melt, as well as volatile fission product materials (e.g. Te). The core melt was produced in situ over the basemat by melting the powders from the top down after establishing an electrical conduction path near the top centre of the powder bed. The inserts were included in the design so that metals and volatile fission products would be introduced into the melt at the last stage of the melt production process in order to minimise early oxidation of clad material and fission product release during the heat-up phase that typically took 2 to 3 hours. Onset of concrete attack was defined by the signal from a thermocouple located at the top surface of the concrete near the axial centreline. The maximum allowable axial ablation depth for the facility was 13 cm; the typical operating time to achieve this ablation depth was 1 hour.

![Figure 2.3-12: Schematic (left) and photograph (right) of the ACE MCCI test apparatus](image)

The facility was heavily instrumented with a complete gas and aerosol diagnostics system, temperature measurements in concrete layer and melt pool, electrical power input, and additional support information for the water cooling systems. Given the calculated (water-cooled) heat losses to the sidewall panels of the apparatus, the gross heating power was adjusted online to compensate for sidewall losses so that the net heating rate was maintained at the target level.

As shown in Table 2.3-3, seven experiments were performed. The principal parameters varied in the test matrix were the concrete type, the extent of cladding oxidation, the core type (i.e. PWR vs. BWR with the corresponding control materials and U/Zr ratios), and the net internal heat generation which was generally consistent with decay heat at 2 hours after scram. The corium compositions also included the inactive fission products mockups BaO, La2O3, SrO, CeO2, MoO2, SnTe, ZrTe2, and Ru, along with the absorber materials B4C or Ag/In.

Principal findings from the ACE MCCI tests are summarised as follows. The temperature of the core oxide melts were higher than originally expected, exceeding 2 100°C (2 400 K) in the majority of the tests (see Figure 2.3-13). This trend is in agreement with the SURC-1 and SURC-2 test data (Copus, Blose, Brockmann, Simpson, & Lucero, 1992-1), (Copus, Blose, Brockmann, Simpson, & Lucero, 1992-2) during the early stages of the interaction. Subsequent melting point measurements by
(Roche, Steidl, Leibowitz, Fink, & Sehgal, 1993) indicated that these temperatures are due to the extended solidus-liquidus range for core oxide materials.

Table 2.3-3: ACE Phase C test matrix

<table>
<thead>
<tr>
<th>Test</th>
<th>Concrete Type</th>
<th>Net Heat Generation (W/kg UO₂)</th>
<th>Reactor Type</th>
<th>Initial Clad Oxidation (%)</th>
<th>Control Rod Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>L5</td>
<td>LCS</td>
<td>325</td>
<td>PWR</td>
<td>100</td>
<td></td>
</tr>
<tr>
<td>L2</td>
<td>SIL</td>
<td>450</td>
<td>PWR</td>
<td>70</td>
<td></td>
</tr>
<tr>
<td>L1</td>
<td>LCS</td>
<td>350</td>
<td>PWR</td>
<td>70</td>
<td></td>
</tr>
<tr>
<td>L6</td>
<td>SIL</td>
<td>350</td>
<td>PWR</td>
<td>70</td>
<td></td>
</tr>
<tr>
<td>L4</td>
<td>Serpentine/SIL</td>
<td>250</td>
<td>BWR</td>
<td>50</td>
<td>B4C</td>
</tr>
<tr>
<td>L7</td>
<td>SIL</td>
<td>250</td>
<td>BWR</td>
<td>70</td>
<td>B4C</td>
</tr>
<tr>
<td>L8</td>
<td>LL</td>
<td>350/150</td>
<td>PWR</td>
<td>70</td>
<td>Ag, In</td>
</tr>
</tbody>
</table>

Figure 2.3-13: ACE phase C experiments ablation front locations (left) and melt temperatures (right)

Downward erosion rates (Figure 2.3-13) cluster in two different bands; the first around ~ 4 mm/min (24 cm/h) and the second around ~ 1 mm/min (6 cm/h). This is in the range of the erosion rates measured in the SURC-1 and SURC-2 tests. For tests with the lower erosion velocity, downward heat transfer may be limited by an insulating crust at the melt-concrete interface, or possibly a highly viscous sublayer in the melt.

For the fully oxidised test L5, off-gas analysis showed that no reduction of H₂O or CO₂ concrete decomposition gases occurred. However, H₂ and CO formed in all other tests where metal was present. Downward migration of 25% to 50% of all decompositions gases through the concrete and out the bottom of the unsealed apparatus was reported (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995) for these tests. This behaviour is most likely unprototypic. Gas release for the various experiments increased in direct proportion to the amount of gases present in the different concrete types, as expected.

Because of the high temperatures of the melts, the formation of gaseous SiO through endothermic Zr-SiO₂ reactions was important (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995). This reaction most likely occurred in parallel with exothermic gas phase reactions between Si metal and
concrete decomposition gases. In several experiments the evolved SiO(g) condensed in the cooler upper plenum of the test section where it decomposed into SiO2 and Si, as well as forming silicates with other aerosols. These condensed reaction products nearly plugged the diluter in two of the tests (L2 and L6), both of which utilised siliceous concrete (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995).

Si species also dominated the aerosols for the limestone concrete tests. The release was highest during insert ablation when Zr concentrations in the melts were highest. High Si concentrations in the aerosols were also observed in the SURC-1 and -2 tests. However, in the lower-temperature BETA-II and SURC-4 tests, major amounts of SiO(g) were not produced.

Melt foaming in tests L1 and L5 also occurred; this was due to a combination of appreciable SiO(g) release coupled to high gas release rates from carbonate decomposition. In a real accident foaming may reduce aerosol release and heat transfer to overlying structure. Foam formation in oxidic pool during MCCI has been studied by (Tourniaire, Dufour, & Spindler, 2009).

With the exception of Te the release of fission products was small (less than 1% of the aerosol mass). This is consistent with data from the SURC and BETA tests. Figure 2.3-15 provides a comparison of the fission product elements and U released for the different ACE tests.

2.3.5. MOCKA experiments

The MOCKA experimental programme (Foit, Cron, Fluhrer, Miassoedov, & Wenz, 2012) is currently underway at KIT in Germany. The purpose of the tests is to provide a more in depth investigation of 2D cavity erosion behaviour using a simulant oxide-metal melt in a stratified configuration. These are transient experiments in which additional energy is supplied to the melt by means of alternating additions of thermite and Zr to extend the duration of the interaction. The heat generated by the thermite reaction and exothermic oxidation of Zr was mainly deposited in the oxide phase.

The experiments were performed in using siliceous concrete crucibles with an ID of 25 cm. The thermite reaction produced an initial melt mass of 110 kg, consisting of a 42 kg Fe metal phase and a 68 kg oxide phase (56 wt% Al2O3 and 44 wt% CaO) at a temperature of ~1900°C. The collapsed height of the metal melt was about 13 cm.

The first experiments, MOCKA 1.1-1.3, were carried out to scope out the behaviour of an oxide-metal melt in a cylindrical concrete crucible without external energy addition. Tests 1.1 and 1.2 utilised 1D crucibles, while the balance of the tests (1.3 to 1.7) utilised 2D crucibles. The axial
erosion by the metal melt in test 1.1 reached 3 cm. However, in test 1.2 no axial erosion occurred, and post-test examinations indicated that this was due to the fact that the metal was encased in a ~0.2 mm thick layer of insulating oxide. In the first 2D test (i.e. MOCKA 1.3), axial erosion was more pronounced than radial (see Figure 2.3-15). This observation is consistent with the previous metal-oxide sustained heating tests carried out in the BETA (Alsmeyer, 1987), (Alsmeyer, et al., 1995) and COMET-L (Miassoedov A., et al., 2008) tests.

To avoid the unexpected outcome of the 1.2 test, 3 kg of Zr were deposited near the crucible wall in tests 1.4 and 1.5. The additional energy released during the exothermal Zr oxidation reactions lead to considerably more concrete erosion in both these experiments. The ratio of the axial to the lateral erosion was found to be ~2 in both tests.

To extend the duration of the interaction with the concrete and allow for significant concrete erosion by the oxide as well as the metal in MOCKA 1.6 and MOCKA 1.7, thermite and Zr were successively added to the melt over the course of these experiments. These tests were also performed with a slightly higher initial Zr mass of 4 kg (compared to 3 kg in tests 1.3 and 1.4) positioned along the initial concrete surfaces. Following the initial Zr oxidisation phase, 63 kg of Fe-Al2O3 thermite and 24 kg Zr metal were added to the melt at 11 minutes into the experiment sequence for test 1.6. Similarly, 117.5 kg of thermite and 34 kg Zr metal were added after 18 minutes in test 1.7. Since the
energy from the Zr oxidation reactions was mainly deposited in the overlying oxide, significant ablation of the crucible wall occurred. In particular, for test 1.6 the axial-radial erosion depths for the metal phase reached 10 cm and 5 cm, respectively, while for the oxide phase radial erosion reached 4.5 cm. For test 1.7 (Figure 2.3-16), the maximum axial-radial ablation depths reached 15 cm and 6.5 cm, respectively. The average axial ablation rate of ~ 7.7 mm/min for this test was as high as that observed in the COMET-L2 experiment (Miassoedov A., et al., 2008).

In summary, the MOCKA tests have produced overall results similar to the former BETA and COMET-L experiments. However, in contrast to these previous experiments, significant lateral concrete erosion by the oxide melt was observed when chemical energy was added to mock up decay heat in the oxide. The pronounced downward erosion by the metal phase seems to be inherent for melts containing a significant fraction of structural steel.

The programme is currently focused on assessing the influence of concrete reinforcement (rebar) on cavity erosion behaviour since data in this area are sparse. Two tests with reinforced siliceous concrete have been conducted thus far (MOCKA 3.1 and 3.2) (Foït, Fischer, Journeau, & Langrock, 2014). The initial thermite mass in the experiments was ~100 kg; 90 to 150 kg of thermite and Zr were added to the melt over the first 10-20 minutes to extend test duration. Both experiments produced isotropic ablation behaviour (i.e. ~7 cm in MOCKA 3.1 and ~11-12 cm in MOCKA 3.2). Two additional tests were performed under similar conditions but without rebar in the concrete to form a technical basis for assessing the effect of the reinforcement. Unlike the tests with the reinforced concrete, these two counterpart tests yielded anisotropic cavity erosion behaviour similar to that observed in the BETA tests (Alsmeyer, 1987), (Alsmeyer, et al., 1995) (i.e. axial ablation approximately twice radial; see Figure 2.3-17). Additional LCS tests have been recently performed to assess also the effect of the concrete composition (Foït J. J., MCCI on LCS concrete with and without rebars, 2015).

![Figure 2.3-17:](image)

(a) Bottom view of MOCKA test section prior to concrete, and views of post-test debris for (b) MOCKA 3.1 with rebar and (c) MOCKA 3.3 without rebar.

### 2.3.6. HECLA experiments

The HECLA test series (Sevón, et al., 2010) was carried out at VTT Technical Research Centre from 2006 to 2009. These experiments investigated the interaction between metallic melt and concrete by pouring molten stainless steel into cylindrical concrete crucibles. These were transient tests (i.e. external heating to simulate decay heat was not provided). The main objective of the study was to examine the behaviour of concretes containing both siliceous as well as hematite (Fe2O3) aggregates. The latter concrete type is used as a sacrificial material in the EPR reactor pit (Fischer & A. Henning,
2009) in Finland. Data on the high temperature behaviour of hematite concrete during core-concrete interaction is very sparse in the literature.

A schematic diagram illustrating the basic elements of the test facility is provided in Figure 2.3-18. The facility consisted of an induction heating furnace for melting the steel charge and a steel chamber in which the test was carried out. The melt was poured from the furnace into a pre-heated tundish above the steel chamber. The melt flowed from the tundish through a bottom hole into the chamber and fell into the concrete crucible. To prevent hydrogen burns, the steel chamber was inerted with a continuous nitrogen purge.

HECLA-1 was a scoping test to shake down the facility using a small (20.4 kg) steel charge mass at relatively low temperature and limited instrumentation. The last four tests were larger and employed 50 kg of stainless steel melt. The HECLA-1 and -2 crucibles were made of ordinary siliceous concrete, while the last three experiments used hematite concrete. A photograph of the HECLA-5 crucible is provided in Figure 2.3-18. All tests were carried out with 2D crucibles with an ID of 28 cm. The melt pool depth was ~15 cm for the 50 kg steel charge.

Figure 2.3-18: Schematic of HECLA test facility (left); photograph of the HECLA-5 concrete crucible (right)

The concrete materials for the HECLA-4 and -5 experiments were obtained directly from the Olkiluoto 3 EPR construction site. The air content of the HECLA-4 concrete crucible was higher than the prototype, but the HECLA-5 concrete was within the technical specifications for the plant. This concrete contains 44.4 wt% SiO2 and 32.1 wt% Fe2O3. The density of the concrete was 2 620 kg/m³. This is heavier (by ~ 15%) than ordinary siliceous concrete; the difference is due to the Fe2O3. GEMINI2 calculations with the NUCLEA-09_1 database predict that the solidus and liquidus temperature of this concrete are 1 140°C and 1 430°C, respectively; at 1 160°C, 50% of the concrete is molten. The calculated solidus and liquidus temperatures agree with those measured using the differential thermal analysis (DTA) technique (Sevón, et al., 2010).

Measured axial and radial concrete ablation depths as a function of time for the HECLA tests are shown in Figure 2.3-19, while the main parameters and results are summarised in Table 2.3-4. The final ablation depths ranged from 10 to 25 mm, with the exception of HECLA-3, for which no
sidewall ablation took place. This may be a consequence of the slower melt pouring rate that occurred in this test (Sevón, et al., 2010). Comparison of the HECLA-2 ablation results with the three subsequent tests that were performed with the hematite concrete indicates no clear difference in ablation behaviour for the two concrete types. The only minor difference is the sidewall ablation rate, which was slightly higher in HECLA-2. However, due to the relatively small number of tests and scatter in the results, no definitive conclusion can be drawn.

Another important conclusion from the tests is that significant cracking (e.g. from thermal shock) and/or spallation of the concrete did not take place during the melt pour or subsequent metal-concrete interaction. In HECLA-4 a pit was formed at the centre of the basemat where the melt jet impinged. However, the depth of the pit was relatively small (i.e. < less than 15 mm).

To summarise, important information was gained from the HECLA experimental programme on the ablation characteristics and properties of hematite concrete used at the Olkiluoto 3 power plant. Although the number of tests was limited, no dramatic differences in ablation behaviour were found. In addition, chemical composition and mechanical property characteristics for this concrete type were measured; this data is documented in (Sevón, et al., 2010).

**Table 2.3-4: Main parameters and results for the HECLA experiments**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Test</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>HECLA-1</td>
</tr>
<tr>
<td>Concrete type</td>
<td>Siliceous</td>
</tr>
<tr>
<td>Melt mass (kg)</td>
<td>20.4</td>
</tr>
<tr>
<td>Melt temperature (°C)</td>
<td>1 560</td>
</tr>
<tr>
<td>Average basemat ablation rate (mm/s)</td>
<td>0</td>
</tr>
<tr>
<td>Average sidewall ablation rate (mm/s)</td>
<td>0</td>
</tr>
<tr>
<td>Total basemat ablation (mm)</td>
<td>0</td>
</tr>
<tr>
<td>Total sidewall ablation (mm)</td>
<td>0</td>
</tr>
</tbody>
</table>

**2.3.7. MCCI experiments in the SICOPS facility**

Areva’s SICOPS experimental programme ( (Langrock & Hellmann, 2010), (Hellmann & Fischer, 2007), (Hellmann, 2000) was conducted with the objective of resolving two open technical questions
related to core-concrete interaction; namely: i) the influence of high core melt metal content, and ii) 2D (axial vs. radial) power split during core-concrete interaction. This work was conducted to support development of the EPR core-catcher concept that utilises hematite concrete as a sacrificial material in the reactor pit, as well as a zirconia-based refractory layer that backs up this material (Fischer & A. Henning, 2009).

The 1D tests addressed the phenomenology and kinetics of melt – concrete interaction with mixed metallic/oxidic melts under sustained heating conditions. Experiments with prototypic core oxide melt under identical conditions were performed for comparative purposes. The conditions selected in the experiments were consistent with both the early and late phases of the core-concrete interaction. Specific experiment objectives were to: i) evaluate concrete erosion rate as a function of input power for different concrete types, ii) determine the effect of high metal content on the concrete erosion behaviour, and iii) investigate the effect of metal content on melt physicochemical behaviour during the interaction. In addition, non-destructive techniques were used to obtain information on the location and amount of metal in solidified debris samples.

The 2D tests focused on clarifying differences in radial-axial power splits during core-concrete interaction and identifying parameters that influence this ratio. The tests further addressed the following questions: i) does power increase lead to increase of temperature and/or erosion rate, ii) are crusts formed at melt- concrete interface, and iii) does the melt temperature decrease in the long term below the melting point of steel reinforcement.

The SICOPS facility used a high-frequency induction heating method to simulate decay heat, coupled with a cold crucible technique for containment of the core melt material. This technology can be used to produce a broad spectrum of prototypic (UO2-bearing) melts for experiment purposes. The crucibles for the experiments are relatively small-scale; i.e. 10 cm ID. The water-cooled crucible absorbs only a small amount of the electromagnetic energy; typically 80-85% of the induction power is absorbed by the melt (Hellmann & Fischer, 2007). A facility schematic along with a photograph is provided in Figure 2.3-20.

![Schematic and photograph of the SICOPS test facility](image)

Information on melt front progression into the concrete was obtained from embedded thermocouples. Heat losses are calculated from flow-rate and temperature rise of the crucible coolant water; the net heat input into the melt is calculated as the difference between total input power minus crucible cooling losses.

In terms of test results, melt temperature measurements were found to be difficult. Optical pyrometer data for a mixed metal-oxide melt with 21 wt% initial Fe content indicated that the melt
was subcooled by ~150 K (i.e. melt temperature was ~1 830°C versus 2015°C liquidus temperature predicted by GEMINI2/NUCLEA09). However, surface observations indicated that melt convection was quite active at this temperature. At this stage the oxidic melt contained 45 wt% concrete. The heat flux into the concrete was ~100 kW/m2 and the concrete erosion velocity was ~1.4 mm/min. This result confirms observations from earlier MCCI tests in which samples of zirconia-based refractory material were placed inside a prototypic molten corium pool during ongoing interaction with concrete (Hellmann & Fischer, 2007). In those tests, melt compositions covered the range of expected conditions from vessel failure through several hours of ex-vessel interaction. None of the zirconia samples showed any measurable dissolution at the surface. It was therefore concluded that the MCCI pool was subcooled and saturated in zirconia which reduces the driving force to dissolve zirconia to effectively zero.

Concerning thermochemistry aspects, the solubility of metal and oxide phases was below the detection limit of SEM/EDX as well as chemical analysis. Iron oxidation in the nine mixed metal-oxide tests was high (20-45%). These data are from tests using siliceous concrete and are for melt compositions initially containing 10 to 26 wt% Fe; the duration of core-concrete interaction ranged from 10 to 30 minutes. These observations are qualitatively in agreement with findings from the VULCANO VBS U1 and U3 experiments (Journeau C. et al., 2009) where a large fraction of metal was oxidised.

![Figure 2.3-21: Post-test debris configurations for oxidic melt (left) and a mixed oxide-metal melt (right) tests](image)

To compare oxide and mixed oxide-metal melt erosion behaviour, an average erosion depth was evaluated based on post-test debris examinations for all experiments (Figure 2.3-21). These data are shown in Figure 2.3-22 as a function of net input power available for concrete dissolution (Note that the interface area was ~100 cm2). The average erosion rates for oxide and mixed metal-oxide melts are the same within the error bands for the experiments at a given power level. A linear relationship exists, which is consistent with a purely thermal concrete erosion process.
Aside from general aspects of core-concrete interaction behaviour, another key element of the SICOPS test programme was to experimentally confirm that refractory zirconia is effectively inert when in contact with molten core debris over a wide range of ex-vessel conditions. To evaluate the stability of ZrO2 in contact with corium, samples were embedded in the bottom and sides of concrete crucibles made of EPR sacrificial (hematite) concrete (Figure 2.3-23). After these tests, samples were extracted for analysis by SEM. These studies showed that zirconia resists attack by oxidic melt under these MCCI conditions. Material compatibility with metal melt was demonstrated in separate experiments. Figure 2.3-24 illustrates results for SICOPS Test #4 with two fully exposed ZrO2 bottom samples inside EPR sacrificial concrete. This test showed that ZrO2 was neither dissolved nor etched by the melt during the interaction. As noted earlier, these results confirm that the core-concrete interaction establishes a subcooled melt pool that is saturated with respect to refractory components ZrO2 and UO2.

Figure 2.3-22: Concrete erosion velocity versus power level for oxide and mixed oxide-metal SICPOS tests

Figure 2.3-23: Concrete crucibles pre-test showing locations of embedded ZrO2 samples
2.3.8. **VULCANO experiment series**

The VULCANO core-concrete interaction programme has been underway at CEA since 2003. A total of twelve reactor material experiments have been carried out in the facility with sustained heating (Journeau C., et al., 2009), (Journeau, et al., 2012). These experiments have focused on addressing two unresolved issues related to long-term core concrete interaction behaviour; namely, radial-axial power split, and the effect of high metal content on cavity erosion behaviour.

A schematic that illustrates principal features of the facility is shown in Figure 2.3-25. Key elements consist of furnaces for melting the corium constituents and an instrumented concrete test section. The oxide phase for a given experiment is produced in a rotating plasma furnace. The corium constituents are loaded as powders into the furnace and held in place by centrifugal force. A plasma arc is initiated between two graphite electrodes located along the axial centreline of the furnace, and the powders are gradually melted by the heat produced from this arc. After melting is completed, the furnace is tilted and the oxide flows down a trough and into the concrete test section. Induction furnaces are used to produce molten metal constituents, and they are introduced into the test section in the same manner.

![Figure 2.3-24: Images of SICOPS test 4 with fully exposed ZrO₂ bottom samples embedded in EPR™ sacrificial concrete](image)

![Figure 2.3-25: Schematic of VULCANO test facility](image)

The concrete test sections are cylindrical half-sections (180° sectors) with an ID of 30 cm and depth of 25 cm. The test sections are heavily instrumented with more than 100 Type K-type
thermocouples that are primarily used to monitor cavity erosion. Several high temperature (Type C) thermocouples are also installed to monitor the melt pool temperature. Four parallel induction coils surround the test section to provide sustained heating that simulates decay heat (Figure 2.3-25). Free (unconnected) coils are also installed beneath the test section to provide electromagnetic shielding for the lower part of the melt where the metal phase eventually segregates. The concrete crucible is a half-cylinder with the dividing wall made of refractory material (MgO in first tests and ZrO₂ thereafter). Part of the input power is lost by conduction through the refractory wall and is removed by coolant water flow through the induction coil. This allows a heat balance to be performed and so the gross input power can be adjusted to offset losses, thereby maintaining input power at the target level.

One of the primary parameters varied in the experiment series has been concrete type, with a total of five different compositions tested (Table 2.3-5). Compositions F and G are reflective of silica and limestone-rich plant structural concretes, respectively, while concrete E represents the sacrificial hematite concrete used in the EPR reactor pit at Olkiluoto, Finland. Concrete C is a specific formulation using calcined limestone ("clinker") aggregate that is similar to Concrete G, but without carbon dioxide. Mortar F is mortar with a composition similar to Concrete F but without the larger aggregates (gravel).

<table>
<thead>
<tr>
<th>Designation</th>
<th>wt% Constituent:</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>CaO</td>
</tr>
<tr>
<td>Concrete C</td>
<td>49.2</td>
</tr>
<tr>
<td>Concrete E</td>
<td>12.7</td>
</tr>
<tr>
<td>Concrete F</td>
<td>16</td>
</tr>
<tr>
<td>Mortar F</td>
<td>18</td>
</tr>
<tr>
<td>Concrete G</td>
<td>42</td>
</tr>
</tbody>
</table>

As shown in Table 2.3-6, six 2D core oxide experiments using four different concrete types have been carried out thus far in the VULCANO facility. The first four experiments (i.e. VB-U4, U5, U6, and U7) utilised traditional plant concretes, while the last two (VBES-U2, -U3) used specialty concretes for separate effect testing. The tests used 28 to 55 kg of prototypic oxidic corium, yielding collapsed pool depths of 16 to 25 cm. The pools were heated for several hours while cavity erosion behaviour was observed.

Before the start of the VULCANO (Journeau C., et al., 2009), (Journeau, et al., 2012) and NEA MCCI CCI (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) experimental programmes, it was generally assumed that concrete ablation would proceed at roughly the same rate in the radial and axial directions. However, both programmes have shown that radial ablation is significantly more pronounced for siliceous concrete. This behaviour is illustrated in Figure 2.3-26 which shows the final cavity profiles for tests VB-U7 and VBES-U2; these tests utilised concretes E and C, respectively. The cavity shapes are quite similar. In tests with the F, E and C concrete types, ablation was initially asymmetric (radial/axial ablation ratio between 2 and 3). However, in the long-term the asymmetry decreased. On the other hand, tests with limestone concretes (VB-U6 and CCI-2 (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) exhibit more isotropic ablation. The final radial and axial ablation depths for all seven oxidic corium experiments are shown in Figure 2.3-27. Except for tests VB-U6 and VBES-U3, all tests experienced more radial ablation than axial. The fact that similar behaviour has been observed in two experiment facilities (VULCANO and CCI) operating at two different scales (50-1 000 kg), with different geometries and heating techniques (induction vs. direct electrical...
heating) indicates that the trend is most likely not an experiment artefact, but rather a real phenomenological affect.

Table 2.3-6: VULCANO 2D oxidic corium test matrix

<table>
<thead>
<tr>
<th>Test</th>
<th>VB-U4</th>
<th>VB-U5</th>
<th>VB-U6</th>
<th>VB-U7</th>
<th>VBES-U2</th>
<th>VBES-U3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Concrete</td>
<td>Concrete F</td>
<td>Concrete F</td>
<td>Concrete G</td>
<td>Concrete E</td>
<td>Concrete C</td>
<td>Mortar F</td>
</tr>
<tr>
<td>Initial mass</td>
<td>45 kg</td>
<td>28 kg</td>
<td>31 kg</td>
<td>54 kg</td>
<td>45 kg</td>
<td>29 kg</td>
</tr>
<tr>
<td>Initial temperature</td>
<td>~2 200 K</td>
<td>~2 400 K</td>
<td>~2 400 K</td>
<td>~2 500 K</td>
<td>~2 500 K</td>
<td>~2 500 K</td>
</tr>
<tr>
<td>Average net power</td>
<td>14 kW</td>
<td>12.5 kW</td>
<td>9 kW</td>
<td>22 kW</td>
<td>15 kW</td>
<td>9.4 kW</td>
</tr>
<tr>
<td>Heating duration</td>
<td>1 h 40</td>
<td>2 h 30</td>
<td>2 h</td>
<td>2 h 40</td>
<td>3 h</td>
<td>2 h 40</td>
</tr>
<tr>
<td>Ablation Pattern</td>
<td>anisotropic</td>
<td>anisotropic</td>
<td>~isotropic</td>
<td>anisotropic</td>
<td>anisotropic</td>
<td>Limited ablation</td>
</tr>
</tbody>
</table>

An examination of the VULCANO database suggests that the superficial gas velocity during core-concrete interaction is not the primary parameter controlling changes in ablation behaviour for the two concrete types. For a given ablation rate, superficial velocity is only a function of the quantity of gas generated per unit volume of ablated concrete (see Table 2.3-7). It is unlikely that there is a sharp transition between concrete types C and G. Small scale simulant material ÉCLAIR experiments (Journeau & Haquet, 2009) indicate that the transition between highly anisotropic natural convection heat flux distributions to an isotropic distribution occurs at quite low superficial gas velocity, and this velocity is below that estimated for all VULCANO tests.

Aside from volumetric flow rate, there is a significant difference in gas mass flow rate between tests that yielded anisotropic ablation patterns and test VB-U6 with concrete G that yielded isotropic ablation. The higher mass flux for concrete G is due to the differences in molar mass between carbon dioxide and steam. Note that models used to describe convection induced by gas sparging (Bonnet, 2000), (Tourniaire, 2006) are currently based on gas volumetric flow rate (superficial velocity) and not the volumetric mass flow rate. The data further indicate that the composition of molten concrete (which affects the corium solidus-liquidus temperatures, viscosity, chemical diffusivity, and ability to dissolve corium crusts (Carenini, Haquet, & Journeau, 2007) is not a major factor influencing ablation behaviour. This is evidenced by the fact that tests VB-ESU2 and VB-U6 yielded very different erosion patterns, yet the non-volatile constituents of the concretes used in these tests (i.e. C and G; see Table 2.3-6) are very similar.

---

3. The threshold corresponds to Reynolds number (calculated with bubble diameter and superficial gas velocity as characteristics length and velocity) around unity.
4. The gas mass flux during core-concrete interaction is equal to the product of the volatile gas mass density in the concrete and the ablation rate.
Post-test examinations indicate that silica gravel may remain in a solid state in the corium pool. On the other hand, limestone (CaCO$_3$) is destroyed by decarbonisation around 700°C to form a fine lime (CaO) powder that was not found in the solidified corium pool for test VB-U6. Moreover, chemical analysis of samples collected from tests VB-U4 and VB-U5 indicate that the pool composition was depleted in silica. This finding indicates that the larger aggregate had not mixed with...
the corium while the finer mortar did. Another finding from the post-test analyses is the presence of corium-rich and corium-lean zones in the pool, indicating that solutal convection was occurring leading to large local variations in composition (see Figure 2.3.29) and thus in density.

![Figure 2.3-28: Composite SEM micrograph from test VB-ESU2 (lower part of the bulk melt)](image)

In addition to purely oxidic tests, four metal–oxide (VBS series) experiments have been carried out in the VULCANO programme; one with limestone concrete, and three with siliceous (see Table 2.3-8). Experiment VBS-U2 was repeated as VBS-U3 due to a poor induction coupling in the first experiment. The tests were conducted by first pouring oxide into the cavity, and then 2 to 3 litres of molten stainless steel are added from several separate steel furnaces. At this point, induction heating is applied to simulate decay heat in the oxidic phase.

One of the most surprising results from the VBS-U1 experiment with limestone concrete was the fact 90% of the initial steel mass of 15 kg was apparently oxidised (i.e. only 1.5 kg steel was recovered following the test). Pre-test calculations predicted an iron oxidation rate of 4% per hour. In order to oxidise 90% of the steel, it would have been necessary for all concrete decomposition gases from ablated concrete to react with the steel, as well as all the gases from the balance of the crucible that reached a temperature of 100°C. The remaining metal was completely depleted in Cr and significantly enriched in Ni (viz. depleted in Fe). Zones in the oxide phase were enriched in concrete oxides (CaO, SiO2) as well as steel oxides. Steel oxidation mechanisms are still under investigation at CEA.

### Table 2.3-8: VULCANO 2D metal-oxide corium test matrix

<table>
<thead>
<tr>
<th>Test</th>
<th>VBS-U1</th>
<th>VBS-U2</th>
<th>VBS-U3</th>
<th>VBS-U4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Concrete</td>
<td>Concrete G</td>
<td>Concrete F</td>
<td>Concrete F</td>
<td>Concrete F</td>
</tr>
<tr>
<td>Oxide load</td>
<td>Corium 2</td>
<td>Corium 2</td>
<td>Corium 2</td>
<td>Corium 1</td>
</tr>
<tr>
<td>Initial oxide mass</td>
<td>35 kg</td>
<td>18 kg</td>
<td>36 kg</td>
<td>35 kg</td>
</tr>
<tr>
<td>Initial stainless steel mass</td>
<td>15 kg</td>
<td>17 kg</td>
<td>15 kg</td>
<td>24 kg</td>
</tr>
<tr>
<td>Heating duration</td>
<td>4 h</td>
<td>Poor coupling</td>
<td>4 h</td>
<td>2 h 25</td>
</tr>
</tbody>
</table>

¹Corium 1 consists of 45/19/20/15 wt% UO₂/ZrO₂/SiO₂/Fe₂O₃, while Corium 2 consists of 69/17/6/7/1 wt% UO₂/ZrO₂/SiO₂/Fe₂O₃/CaO.
In the tests with siliceous concrete, steel oxidation also occurred but to a lesser extent in comparison to the limestone concrete case. As for VBS-U1, the remaining steel was depleted in Cr and enriched in Ni. The most surprising result from the three tests with siliceous concrete is that spatial segregation occurred, but it was not totally driven by gravity. In particular, steel layers were found on both horizontal as well as vertical surfaces (Figure 2.3-29). Solidified metal drops were also found in the oxide pool; sizes ranged from 0.1 mm up to several centimetres. Oxidic melt and un-melted silica aggregate were also trapped in some parts of the metal layer.

For test VBS-U3 the metal layer did not cover the entire horizontal and vertical concrete surfaces, but only an angular sector of slightly less than 90°. Concrete temperatures showed that axial and radial ablation was more pronounced in the areas where metal was found. This is consistent with other oxide-metal simulant tests (e.g. BETA (Alsmeyer, 1987), (Alsmeyer, et al., 1995) that have shown enhanced heat transfer at the metal-concrete interface relative to the oxide-concrete interface. Further note that tests VBS-U3 and VBS-U4 were carried out with different corium compositions that had significant density differences due to variations in initial concrete content (see Table 2.3-8). This indicates that the unique metal phase topology is not strongly linked to the oxide phase density. Due to ongoing solutal convection, plumes with various concrete fractions – and thus different densities – are present below the metallic material. For instance, analysis of a sample from test VBS-U3 indicates estimated densities (evaluated at the pool temperature) of 6.2 kg/l for the metal, 6.6 kg/l for the concrete-lean oxide, and 4.6 kg/l for the concrete-rich plume. This analysis indicates that phenomenological behaviour near the core-concrete interface is more complex that that observed in simulant material stratification experiments where only two liquid compositions are present. During core-concrete interaction, miscible but yet unmixed oxides are present and coexist with immiscible molten metal.

The four oxide-metal tests performed in the VULCANO facility are the first significant scale experiments with a prototypic oxide-metal composition with decay heat simulation in the oxide phase. In other experiments such as COTELS ( (Nagasaka, et al., 1999), (Nagasaka, et al., 1999), (Zhdanov, et al., 1999), (Farmer, Kilsdonk, & Aeschlimann, 2009), the heat was injected into the metal phase due to characteristics of the induction system used in those tests. The VULCANO VBS tests have identified unexpected phenomenology regarding metal oxidation during core-concrete interaction, as well as the spatial distribution of the metal due to segregation. These phenomena need to be better understood in order to determine how to scale the results to plant conditions.

**Figure 2.3-29:** Photograph of large metallic structure recovered from test VBS-U4
2.3.9. NEA MCCI Project CCI experiments

The NEA MCCI Project was carried out at ANL from 2002 through 2010 (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010). As part of this work, a total of six large-scale reactor material core-concrete interaction experiments were carried out with sustained heating. One of the primary objectives of these tests was to provide data on cavity erosion behaviour and melt temperature response in order to support code verification and validation activities. To augment the amount of information obtained from these tests, five of the six experiments were flooded from above after a pre-defined concrete ablation depth was reached in order to provide information on the nature and extent of debris coolability processes. This section summarises information gained from the tests related to dry cavity erosion for siliceous and limestone-common sand concrete types; the coolability results are summarised in Section 2.4. Specifications for all six tests in the experiment series are shown in Table 2.3-9, tests CCI-1 through CCI-5 are relevant to the current discussion. Key facility features are illustrated in Figure 2.3-30, while details of the test section are shown in Figure 2.3-31.

The facility consisted of a test apparatus, a power supply for direct electrical heating (DEH) of the corium, a water supply system, two steam condensation (quench) tanks, a ventilation system to filter and exhaust the reaction product gases, and a data acquisition system. The apparatus for containment of the core material consisted of a test section that was ~ 3.4 m tall. The internal dimensions of the bottom test crucible varied in the test series, but the nominal configuration was a square internal cross-section measuring 50 x 50 cm (Figure 2.3-31). The lower section was basically a notch-type configuration in which the basemat and two sidewalls experienced concrete erosion, while the other two opposing walls were lined with tungsten electrodes that were used to provide DEH to the melt to simulate decay heat. The two electrode walls were made from refractory MgO; the electrodes were backed up with a layer of crushed UO2 pellets to provide additional protection against attack by the corium. Melt generation in all experiments was through an exothermic chemical reaction yielding the target initial melt mass over a timescale of ~ 30 seconds. After the reaction, DEH was supplied at a level that provided a heat flux in the range of 150 (for CCI-2 through -5) to 200 (for CCI-1) kW/m² to all melt surfaces in contact with concrete (as well as upper atmosphere) at the start of the experiment. This power level range is consistent with conditions at ~ 2 hours following scram in a PWR. The concrete basemat and sidewalls of the apparatus were instrumented with Type K thermocouples to measure ablation front progression and Type C units (in tungsten thermowells) to measure melt temperature.
Initial melt compositions, temperatures, and corresponding power levels for the various tests are summarised in Table 2.3-9. All tests were carried out with fully oxidised PWR core melt with the exception of CCI-4. This test utilised a BWR composition in which 78% of the cladding was initially oxidised; the melt also contained 7.7 wt% stainless steel. This steel fraction is less than half that used in the VULCANO metal-oxide tests (Journeau C., et al., 2009), (Journeau, et al., 2012) (see Section 2.3.8), and so this test can be considered as an intermediate data point when viewed in context with the VULCANO results.

**Figure 2.3-31:** Side (left) and top (right) views of the CCI test section

**Table 2.3-9:** Specifications for CCI tests

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Specifications for Test:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Corium</td>
<td></td>
</tr>
<tr>
<td>CCI-1</td>
<td>100% oxidised PWR + 8 wt% SIL</td>
</tr>
<tr>
<td>CCI-2</td>
<td>100% oxidised PWR + 8 wt% LCS</td>
</tr>
<tr>
<td>CCI-3</td>
<td>100% oxidised PWR + 15 wt% SIL</td>
</tr>
<tr>
<td>CCI-4</td>
<td>78% oxidised BWR with 7.7 wt% SS and 10 wt% LCS²</td>
</tr>
<tr>
<td>CCI-5</td>
<td>100% oxidised PWR + 15 wt% SIL</td>
</tr>
<tr>
<td>CCI-6</td>
<td>100% oxidised PWR + 6 wt% SIL</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Concrete type²</th>
<th>SIL (US-type)</th>
<th>LCS</th>
<th>SIL (EU-type)</th>
<th>LCS</th>
<th>SIL (EU-type)</th>
<th>SIL (EU-type)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Basemat cross-section</td>
<td>50 cm x 50 cm</td>
<td>50 cm x 50 cm</td>
<td>50 cm x 40 cm</td>
<td>50 cm x 79 cm</td>
<td>70 cm x 70 cm</td>
<td>70 cm x 70 cm</td>
</tr>
<tr>
<td>Initial melt mass (depth)</td>
<td>400 kg (25 cm)</td>
<td>400 kg (25 cm)</td>
<td>375 kg (25 cm)</td>
<td>300 kg (25 cm)</td>
<td>590 kg (25 cm)</td>
<td>900 kg (28 cm)</td>
</tr>
<tr>
<td></td>
<td>Other walls: concrete</td>
<td>Other walls: concrete</td>
<td>Other walls: concrete</td>
<td>Other walls: concrete</td>
<td>Other walls: concrete</td>
<td>Other walls: concrete</td>
</tr>
<tr>
<td>Lateral/Axial ablation limit</td>
<td>35/35 cm</td>
<td>35/35 cm</td>
<td>35/35 cm</td>
<td>45/42.5 cm</td>
<td>40/42.5 cm</td>
<td>24/32.5 cm</td>
</tr>
<tr>
<td>Initial melt temperature</td>
<td>1 950 °C</td>
<td>1 880 °C</td>
<td>1 950 °C</td>
<td>1 850 °C</td>
<td>1 950 °C</td>
<td>2 300 °C</td>
</tr>
<tr>
<td>System pressure</td>
<td>Atmospheric</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Melt formation tech.</td>
<td>Chem. reaction (~30 s)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Melt heating technique</td>
<td>Constant @ 150 kW</td>
<td>Constant @ 120 kW</td>
<td>Constant @ 120 kW</td>
<td>Constant @ 95 kW</td>
<td>Constant @ 145 kW</td>
<td>Constant @ 210 kW</td>
</tr>
</tbody>
</table>

**Table 2.3-9:** Specifications for CCI tests
### Criteria for water addition

1) 5.5 hours of operation 2) ablation → 5 cm of limit

1) 5.5 hours of operation 2) ablation → 5 cm of limit

1) 5.7 hours of operation 2) ablation → 5 cm of limit

1) 6.0 hours of operation 2) ablation → 5 cm of limit

2.5 cm concrete ablation

### Timing of water addition (actual)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Specifications for Test:</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>CCI-1</td>
</tr>
<tr>
<td>Inlet water flowrate and temperature</td>
<td>2 lps/20 ºC</td>
</tr>
<tr>
<td>Water depth over melt</td>
<td>50 ± 5 cm</td>
</tr>
<tr>
<td>Power supply operation after water addition</td>
<td>Constant voltage</td>
</tr>
<tr>
<td>Test termination criteria</td>
<td>1) $T_{\text{melt}} &lt; T_{\text{con,sol}}$, 2) ablation arrested, or 3) ablation → limit</td>
</tr>
<tr>
<td>Operational Summary</td>
<td>Pronounced lateral erosion in one sidewall; successful cavity flooding</td>
</tr>
</tbody>
</table>

*a* SIL denotes siliceous concrete, LCS denotes Limestone/Common Sand concrete.

*b* After erosion of concrete/metal inserts and at start of basemat ablation.

Comparisons of key test results for the CCI tests are provided in Figure 2.3-32 and Figure 2.3-33 that provide the average melt temperature and peak lateral and axial concrete ablation rates for each of the tests. Initial melt temperatures were in the range of 1880-1950ºC; differences were due to uncertainty/variability in the reaction temperatures for the different chemical mixtures used to generate the melt pools. During dry cavity operations, all tests exhibited the overall trend of decreasing melt temperature as ablation progressed, which was due to a heat sink effect as relatively cool concrete slag was introduced into the melt, as well as the increasing heat transfer surface area as the melts expanded into the concrete crucibles. The decline in melt temperature may further reflect the evolution of the pool boundary freezing temperature that decreased as additional concrete was eroded into the melt during the tests (Roche, Steidl, Leibowitz, Fink, & Sehgal, 1993).

![Figure 2.3-32: Average melt temperatures for the CCI tests](image-url)
One objective of the test series was to investigate the effect of unoxidised Zr cladding on the thermalhydraulics of the core-concrete interaction. As noted earlier, CCI-4 was initiated with 22% of the Zr initially present in an unoxidised state. As is evident from Figure 2.3-32, the effect of the oxidation reaction between Zr and sparging concrete decomposition gases (CO2, H2O) was to cause an exothermic transient in which the melt temperature increased by ~ 100 °C during the first 20 minutes of the test. This same type of transient was observed in previous metal tests conducted in SNL (Copus, Blose, Brockmann, Gomez, & Lucero, 1990), (Copus, Blose, Brockmann, Gompex, & Lucero, Core-Concrete Interactions using Molten Steel with Zirconium on a basaltic Basemat: The SURC-4 Experiment, 1989) and at KIT in the BETA facility (Alsmeyer, 1987), (Alsmeyer, et al., 1995). In addition, the effect of Zr oxidation was also investigated as part of the ACE/MCCI core oxide test series (Thompson D. H., Fink, Armstrong, Spencer, & Sehgal, 1992). Unfortunately, tests conducted in those experiments with partially oxidised melt and LCS concrete were of very short duration (i.e. several minutes), and so the long term effects of the oxidation reactions on thermalhydraulic behaviour were not clear. However, CCI-4 ran for several hours past the point at which all the cladding had oxidised. Moreover, CCI-2 can be considered a counterpart experiment insofar as cladding oxidation is concerned. Comparison of the results indicates that cladding oxidation reactions cause an early exothermic temperature transient in the melt, and after the reaction is complete, the temperature drops to that consistent with fully oxidised melt conditions.

Additional examination of Figure 2.3-32 indicates that CCI-1 exhibited slightly different melt temperature behaviour compared to the other fully oxidised tests. In this test, the melt temperature was relatively constant over the first ~40 minutes of the interaction. One possible contributor to this trend was the fact that this test was run at a 25% higher power level in comparison to the other CCI tests. However, the lack of a temperature decline may have also been caused by crust formation at the core-concrete interfaces that acted to insulate the melt. Relatively low heat transfer rates to the concrete boundaries were evidenced by the low ablation rates exhibited over the first 40 minutes. Note that this type of behaviour is consistent with other transient core oxide tests carried out at Sandia (Gronager, Suo-Anttila, & Brockmann, 1986), wherein sub-stoichiometric melt [(U,Zr)O2-x] was dropped into basalt and limestone-common sand concrete test sections and allowed to cool with no further heating. In these tests, no concrete ablation occurred and the conclusion was drawn that crusts acted to thermally protect the concrete. However, in the current tests the melts were continuously heated. Thus, once the surface crusts failed in CCI-1, ablation proceeded vigorously and the melt temperature fell rapidly in comparison to the other tests. This initial stable crust behaviour may have been linked to the exceptionally low gas content for this concrete type in comparison to others used in the test series (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006). For instance, gas sparging at the core-concrete interface may exert mechanical force(s) on the crust thereby freeing the material and allowing ablation to proceed. If this interpretation is correct, then the reduced gas production rate may have allowed the
crust to remain stable over an extended period of time in CCI-1, which in turn caused the melt temperature to increase.

Aside from CCI-1, other CCI tests also provided evidence of early crust formation that influenced ablation behaviour (Figure 2.3-33). For CCI-2, both axial and lateral ablation rates were quite low and the melt temperature relatively constant until ~ 30 minutes, after which time a period of rapid erosion occurred. However, unlike CCI-1, these erosion bursts were not sustained. Rather, after ~ 5 cm of ablation, both the axial and lateral ablation rates slowed significantly and approached quasi-steady states. The reduced crust stability period for CCI-2 is consistent with the idea that gas sparging can disrupt surface crusts, since the gas content of the CCI-2 concrete was significantly greater in comparison to CCI-1.

Aside from initial crusting effects, examination of Figure 2.3-33 indicates that the long-term ablation process is influenced by concrete type. Estimates of average lateral and axial ablation rates for the five tests are provided in Table 2.3-10; the Table also includes estimates of the corresponding concrete heat fluxes that were formulated based on the average erosion rates (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010). As shown in the table, long-term lateral and axial ablation rates for Tests CCI-2 and CC-4, both of which were conducted with LCS concrete, were about the same. For CCI-2, the concrete erosion rate averaged 4 cm/hr over several hours of interaction before gradually decreasing; the corresponding surface heat flux was ~ 60 kW/m². For CCI-4, the fluxes were slightly lower (i.e. ~ 40 kW/m²), but this is due to a surface scaling effect as the initial cavity size and therefore input power level were reduced to expand the test duration. Thus, the lateral/axial heat flux ratios for these LCS tests are ~1.

The relatively uniform power splits for CCI-2 and CCI-4 can be contrasted with the results of the three tests conducted with siliceous concrete. For test CCI-1, the ablation was highly non-uniform, with most of the ablation concentrated in the north sidewall of the test apparatus. As described above, the results for this experiment were felt to be strongly influenced by transient crust effects, and so a power split estimate was not formulated for this test. However, tests CCI-3 and CCI-5 seemed to exhibit repeatable, albeit non-isotropic, ablation behaviour. For CCI-3, fairly symmetrical ablation occurred insofar as the progression of the ablation fronts into the two opposing sidewalls of the apparatus is concerned. However, unlike Test CCI-2, the lateral ablation was highly pronounced in comparison to axial for this particular test. A similar trend was noted for CCI-5 that was conducted with a single siliceous concrete sidewall. As shown in Table 2.3-10, lateral ablation averaged 10 cm/hr during the late phases of the CCI-3 and CCI-5 experiments, while the axial ablation rate was limited to 2.1 to 2.5 cm/hr over the same time frame for the two tests. On this basis, the lateral/axial surface heat flux ratios for tests CCI-3 and CCI-5 were estimated as ~ 4 and ~ 4.7, respectively. These values are significantly higher than the near-unity ratios deduced for tests CCI-2 and CCI-4 with LCS concrete. Thus, the data suggests that there is an effect of concrete type on the spatial heat flux distribution at the core-concrete interface during dry core-concrete interaction. This same overall trend was observed and documented in counterpart experiments performed in the VULCANO test facility (Journeau, et al., 2012), (Langrock & Hellmann, 2010) (see Section 2.3.8).
Table 2.3-10: Lateral/axial ablation rate and power split estimates for CCI tests

<table>
<thead>
<tr>
<th>Test</th>
<th>Concrete Type</th>
<th>Lateral ablation</th>
<th>Axial ablation</th>
<th>Lateral-axial heat flux ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Ablation Rate</td>
<td>Heat Flux</td>
<td>Ablation Rate</td>
</tr>
<tr>
<td></td>
<td></td>
<td>(cm/hr)</td>
<td>(kW/m²)</td>
<td>(cm/hr)</td>
</tr>
<tr>
<td>CCI-1</td>
<td>SIL (US)</td>
<td>N: 39.1</td>
<td>395</td>
<td>26.1&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td></td>
<td></td>
<td>S: 8.4</td>
<td>86</td>
<td></td>
</tr>
<tr>
<td>CCI-3</td>
<td>SIL (EU)</td>
<td>10.0</td>
<td>97</td>
<td>2.5</td>
</tr>
<tr>
<td>CCI-5</td>
<td>SIL (EU)</td>
<td>9.8</td>
<td>95</td>
<td>2.1</td>
</tr>
<tr>
<td>CCI-2</td>
<td>LCS</td>
<td>4.0</td>
<td>58</td>
<td>4.0</td>
</tr>
<tr>
<td>CCI-4</td>
<td>LCS</td>
<td>2.7</td>
<td>39</td>
<td>2.8</td>
</tr>
</tbody>
</table>

<sup>a</sup> Heat flux ratio not evaluated for this test due to large asymmetry in lateral cavity erosion.

<sup>b</sup> Ablation burst; rate appeared to slow significantly after this time interval; see Figure 2.3-33.

Another finding from the CCI tests was that the nature of the core-concrete interface was noticeably different for limestone tests in comparison to siliceous; see Figure 2.3-34. The ablation front for the siliceous concrete tests consists of a region where the core oxide had locally displaced the cement that bonded the aggregate. Conversely, for Test CCI-2 the front consisted of a powdery interface in which the core and concrete oxides were clearly separated. The interface characteristics may have influenced the heat transfer at the interface, thereby resulting in different ablation behaviour for the two concrete types. Differences in interfacial conditions for these two concretes were also found in the VULCANO tests (Journeau, et al., 2012), (Langrock & Hellmann, 2010).

Finally, one of the objectives for test CCI-5 was to examine the influence of melt pool aspect ratio on the radial/axial power split under dry cavity conditions. In this experiment the test section aspect ratio (i.e. test section width/melt depth) was increased to the greatest extent possible to more accurately mock up prototypic conditions. The approach was to modify the test section design to include a single concrete sidewall that would undergo ablation, whereas the opposing sidewall was made from insulating material (MgO lined with UO₂). With this approach the aspect ratio was increased from a value of ~1 for CCI-3 to ~3.2 for CCI-5. The relatively close agreement in long-term ablation behaviour for tests CCI-3 and CCI-5 (Figure 2.3-33) indicates that aspect ratio has little influence on ablation characteristics. This observation lends additional credibility to observation of skewed power splits for siliceous concrete observed in both the CCI and VULCANO tests. Finally, data from the Chernobyl accident, involving core melt interaction with silica-rich (basalt) concrete over several days, also support this observation (see Section 2.5). Aside from overall cavity erosion behaviour, video data indicated that a crust was present over the melt surface during a large fraction of dry cavity ablation phase for all tests. The crust contained vent openings that allowed eruptions to occur as the tests progressed. The frequency and intensity of the eruptions was directly correlated to concrete gas content. In terms of the chemical analyses conducted as part of the test series, samples were collected to: i) characterise the lateral and axial composition variations of the solidified debris, and ii) characterise the composition of corium regions that played key roles in the coolability aspects of the tests (e.g. porous crust zones formed at the melt/water interface, and the material erupted after cavity flooding in CCI-2). Analysis of samples taken to characterise the lateral composition variation indicate that for most tests, the corium in the central region of the debris had a higher concentration of core oxides in comparison to samples collected near the two ablating concrete sidewalls. Conversely, samples taken to characterise the axial composition variation indicated the general trend of slightly increasing core oxide concentration as the concrete surface is approached. For all three tests conducted with siliceous concrete, two zones were present: a heavy monolithic oxide phase immediately over the
basemat that was enriched in core oxides, with a second overlying porous, light oxide phase that was enriched in concrete oxides (see Figure 2.3-34).

Figure 2.3-34: Axial debris morphology for siliceous concrete tests (a) CCI-1, (b) CCI-3, and (c) CCI-5, and LCS Test (d) CCI-2 (All tests were flooded except CCI-5)

2.4. Flooded cavity MCCI experiments

The previous sections have outlined phenomenology and supporting experiments related to core-concrete interaction under dry cavity conditions. This section summarises results of experimental programmes that have focused on studying the nature and extent of core debris cooling that occurs when the core debris is flooded from above; experimental programmes that have addressed this issue are summarised in Table 2.2-2. Alternative cooling strategies and associated experimental programmes for stabilising core debris that do not rely on top flooding (i.e. core-catchers) are briefly summarised in Section 2.2.

2.4.1. SNL flooded cavity experiments

Although the SNL R&D programme on core-concrete interaction primarily focused on dry cavity erosion and fission product release, three melt coolability experiments were also conducted with sustained heating and concurrent concrete erosion. These experiments include the SWISS tests (Blose, Gronager, Suo-Anttila, & Brockman, 1987), conducted with stainless steel melts interacting with limestone/common sand concrete, and the WETCOR test (Blose, et al., 1993) conducted with an oxide simulant that also interacted with LCS concrete.

The SWISS apparatus consisted of an interaction crucible, melt generator, and a water delivery system. The system also contained instrumentation to measure aerosol and non-condensable gas release both before and after water was added to the test section. The MgO interaction crucible was cylindrical with an ID of 21.6 cm. The concrete basemat was located at the bottom of the crucible. The melt was produced by induction heating of a 46 kg 304 stainless steel charge mass in a melt generator located over the crucible. After the melt was formed, it was dropped into the crucible to initiate the test. Fission product decay heat was simulated with induction heating of the metal charge. Water was added over the melt at a preselected time, and the depth over the melt was maintained at ~ 30 cm. The heat flux to the overlying water was measured by the mass flow rate of the coolant and the differential temperature rise across the water inlet and outlet plenums. An aerosol detection system was also provided to quantify the extent of fission product scrubbing once water was added over the melt.
For the SWISS-1 test, water was added very late in the sequence due to test occurrences. Thus, this was basically a dry MCCI experiment with minimal coolability data obtained. For SWISS-2, a total of 44.2 kg of melt was delivered to the test section. The initial temperature was between 1530 and 1686°C at the time of delivery. Net input power was maintained at 60 to 70 kW throughout the balance of the test. The corresponding power density was 1500-1700 W/kg of fuel, which is ~ 5 times the anticipated power level of ~ 300 W/kg for a typical PWR at 2 hours into the accident sequence. Water was added to the test section at 1.6 minutes past melt delivery. The peak melt/water heat flux was measured as 1.4 MW/m². After steady state conditions were established, the basemat erosion rate was ~ 27 cm/hour, while the melt-water heat flux was ~ 0.8 MW/m². Comparison of the SWISS-1 and SWISS-2 ablation results indicates that, for these experiments, water addition had little influence on the basemat erosion rate. However, post-test examinations indicated that a bridge crust ranging from 5.1 to 6.4 cm thick had formed. This crust spanned the width of the test section and was anchored to the test section sidewalls. As a result, an intervening gap developed between the melt and crust as the MCCI progressed downward. This non-prototypic gap may have adversely affected the quench progression in this experiment, as indicated by the simulant coolability experiments of Theofanous et. al (Lomperski, Farmer, & Basu, 2006), as well as the MACE reactor material experiments (Farmer, Kilsdonk, & Aeschlimann, 2009). As an important aside, the aerosol data indicated an effective decontamination factor (DF) of ~ 10 for the 30 cm deep saturated water pool maintained over the debris during the test.

The WETCOR test (Blose, et al., 1993) utilised an apparatus similar to that used for the SWISS tests. The MgO interaction crucible was slightly larger, with an ID of 33.0 cm. The basemat was also made from LCS concrete. In contrast to the SWISS tests, the melt was generated by in-situ heating of a 34.1 kg oxide charge (76.8/16.9/4.0/0.9/0.5 wt% Al₂O₃/CaO/SiO₂/Fe₂O₃/MgO). The sidewall heating elements consisted of tungsten susceptor rings which were inductively heated. This approach was intended to eliminate crust anchoring to the test section sidewalls after water addition, as occurred during the SWISS tests, by maintaining the sidewalls above the freezing temperature of the melt. After the melt was generated and concrete ablation established, the cavity was flooded, thus initiating the experiment. Fission product decay heat after water addition was simulated by maintaining input power to the sidewall heating elements.

Key results for the WETCOR test are summarised as follows. The sidewall heating technique resulted in a non-uniform ablation front at the time of water addition; i.e. the ablation depth had reached ~ 4.3 cm at the centre of the test section, 0 cm at the mid-radius, and ~ 2.6 cm along the outer periphery near the tungsten susceptor coils. The cavity was flooded at 529 minutes into the test. The net input power to the melt after water addition was estimated as 28 kW; the corresponding specific power density was ~ 0.61 W/cm³. Addition of water resulted in an initial, intense bulk cooling phase which lasted 1-2 minutes. The thermocouple data suggests that the melt temperature was reduced by ~100°C during this time. Dispersion of melt droplets into the overlying coolant was visible during bulk cooling. After the initial intense heat transfer period, a stable crust formed thereby reducing the heat transfer to the overlying coolant. The melt/water heat flux was estimated as 0.52 ± 0.13 MW/m². After crust formation, the thermocouple data indicated that the temperature stabilised in a range of 1480-1580°C. The sidewalk of the test section failed 26 minutes after water was added, leading to a pressurised melt run-out from the test section. Post-test disassembly revealed the presence of a 3 to 5 cm wide gap between the top crust and the underlying solidified debris. As for the SWISS tests, this finding indicates that the crust had anchored to the test section sidewalk after water was added, which most likely influenced the cooling behaviour. The WETCOR aerosol data indicated an effective DF in the range of 3 to 15 for the ~30 cm deep saturated water pool maintained over the debris during the test.

2.4.2. COMET-L3 experiment

The COMET-L3 experiment (Miassoedov, Alsmeyer, Cron, & Foit, 2010) was carried out at KIT to investigate 2D concrete erosion and debris coolability in a cylindrical crucible made from siliceous
concrete. The facility utilised base hardware and technology that was developed as part of the preceding BETA experimental programs (Alsmeyer, 1987), (Alsmeyer, et al., 1995). The concrete crucible was located at the base of the test rig and was situated over an induction coil that delivered heat to the melt metal phase to simulate decay heat. The cylindrical sidewall above the coolant device (Figure 2.4-1) was formed from a refractory MgO ceramic ring that prevented thermal attack of the upper test rig. The core melt simulant was generated externally and then poured through into the test crucible at an initial temperature of ~1700°C. The initial melt composition consisted of 459 kg metal (90 wt% Fe, 10 wt% Ni) and 159 kg oxide (56 wt% Al2O3, 44 wt% Ca). The material was poured into the concrete cavity through an opening in the lid of the apparatus. This initial charge thus yielded a 25 cm deep metal layer overlaid by 20 cm of oxide in the 60 cm ID concrete crucible. The crucible was heavily instrumented with Type K thermocouples to detect ablation front location; melt surface temperature was measured with an infrared camera.

Figure 2.4-1: Dimensions of the COMET-L3 test section

In terms of the test operating procedure, after the melt was added to the crucible the debris was to be heated at a target power level of 200 kW. The cavity was to be flooded after concrete ablation had proceeded 9 cm either radially or axially. At the end of the dry cavity erosion phase, the melt was to be flooded by water from the top at a rate of 375 l/s.

In terms of the test results, in the initial phase of the test (less than 100 s), the superheated melt underwent an intense interaction with the concrete, leading to a rapid decrease in melt temperature (Figure 2.4-2). During this phase the steel erosion rates in the radial and axial directions were similar (∼0.15 mm/s); see Figure 2.4-3. The erosion rate by the overlying oxide was small in comparison (due in part to the lack of internal heating) and stopped after <1 cm erosion. After the initial transient, the interaction rate slowed and by ∼800 seconds the axial erosion rate fell to ∼0.07 mm/s; the lateral erosion rate was ∼1/3 this value. These rates are consistent with those measured in the COMET-L1 (Alsmeyer, et al., 2005) and COMET-L2 (Miassoedov, Alsmeyer, Cron, & Foit, 2010) experiments, as

5. In terms of difference with reactor case, as the heat is delivered only in the metal phase a constant power remains injected in the melt after flooding because the metal ejection is very unlikely due to the large density difference between metal layer and simulant top oxide layer.
well as the former BETA experiments (Alsmeyer, 1987), (Alsmeyer, et al., 1995) at low power density. During this period burnable gases were released as a result of chemical reaction of steam and CO₂ with iron. The dominant gas was hydrogen.

Surface flooding of the oxide melt was initiated at 800 seconds. The upper melt surface was devoid of crust and moderately agitated by sparging decomposition gases at the time water was added. Flooding did not lead to strong melt/water interaction or penetration of water into the melt. The melt surface was almost completely covered by crust after 60 seconds, and the surface was quenched by 140 seconds. The crust eventually anchored to the cavity sidewalls and separated from the melt below. Concrete erosion continued, although at a reduced rate of ~0.04 mm/s. The melt eventually reached the maximum erosion limit of the crucible, at which time input power was terminated.

Post-test analysis of the solidified melt showed that a large void had developed under the surface crust (Figure 2.4-4). The crust was bulged ~20 cm in the central region, which may have been due to pressure from below due to build-up of concrete decomposition gases. Water ingestion into the bulk of the oxide and underlying metal was prevented by the suspended bridge crust and no melt was ejected trough this crust. No efficient fragmentation mechanism was identified that could breach the crust and contribute to cooling. An energy balance indicated that only about 20% of the input power was transferred through the upper melt surface, intervening gap, and overlying bridge crust into the water. This confirmed that the influence of surface flooding is indeed small in this experiment and unable to stop the downward erosion. However, formation of anchored crusts is not expected at plant scale (Lomperski & Farmer, Corium Crust Strength Measurements, 2009), and results of other experimental programmes (Farmer, Kilsdonk, & Aeschlimann, 2009), (Lomperski, Farmer, & Basu, 2006) have indicated that gap formation can effectively terminate any debris cooling mechanisms that could lead to long-term coolability.

![Figure 2.4-2: Measured melt surface temperatures for COMET-L3](image)

Figure 2.4-2: Measured melt surface temperatures for COMET-L3
2.4.3. ECOKATS-2 experiment

The ECOSTAR Project was carried out in the European Union to investigate sequences that play a key role during the ex-vessel phase of a postulated core-melt accident (Alsmeyer, et al., 2005). As part of this work, the ECOKATS-2 demonstration experiment (Alsmeyer, et al., 2005) was carried out to provide melt spreading data for code qualification purposes. After the material had spread, the interaction was subsequently flooded to provide insights into debris coolability at large scale.

In this experiment, 3200 kg of a metal-oxide mixture was poured at a rate of ~ 20 l/s onto a 2 m x 2 m siliceous concrete basemat. The oxide consisted of a mixture of Al2O3, SiO2, CaO and FeO; the mixture was designed to have a broad freezing range of 1 000 to 1 550°C to emulate that of a prototypic corium (Roche, Steidl, Leibowitz, Fink, & Sehgal, 1993). The metal phase was pure iron.

The melt evenly spread to an equilibrium depth of ~ 20 cm in less than 60 seconds. Strong gas release from the concrete ensued, with hydrogen flames rising from the melt surface. Following the spreading transient, the debris upper surface was flooded at a rate of ~ 4 l/s to examine the cooling behaviour. In spite of ongoing core-concrete interaction with substantial gas release and an agitated melt surface, the flooding process was mild and did not cause intense interactions of melt and water (Figure 2.4-5). The melt surface crusted over and small volcanoes developed that ejected melt from below in the form of lava flows, but particle ejection was not observed. The surface crust was firmly
anchored to the test section sidewalls. Only little concrete ablation has finally been evidenced as the melt was not heated after pouring.

![Figure 2.4-5: ECOKATS-2 during spreading (left) and after surface flooding (right)](image)
The spreading area is 2 m x 2 m

Cool-down of the melt was slow due to the upper crust which limited the ingression of water. Later in the experiment the central part of the crust was lifted by gases from the decomposing concrete. This created a porous surface structure which allowed more efficient cooling of the upper 4 cm of the melt surface, but the balance of the melt material solidified in a dense form that excluded water ingression and efficient cooling. Based on this simulant experiment one may conclude that top flooding alone would be unlikely to stop concrete erosion by corium melt in a plant accident by the water ingestion cooling mechanism. However, it is important to note that subsequent water ingestion measurements made as part of the NEA MCCI Program (Lomperski, Farmer, & Basu, 2006), (Lomperski & Farmer, 2007) indicate that the dry-out limit due to water ingestion is quite high for in-vessel core oxide material. Conversely, the dry-out limit is quite low for concrete slag materials such as the silica-rich oxide simulant used in this spreading experiment.

### 2.4.4. COTELS experiments

Core concrete interaction and debris coolability were studied experimentally within the framework of the COTELS project (Nagasaka, et al., 1999), (Nagasaka, et al., 1999), (Zhdanov, et al., 1999), (Maruyama, Tahara, Nagasaka, & Vassiliev, 2002), (Maruyama, et al., 2006) from 1995 to 2002 as a joint study between NUPEC (Japan) and NNC (Republic of Kazakhstan). The experiments were conducted in the testing complex at NNC using the “LAVA-M” and “LAVA-B” facilities; schematics are provided in Figure 2.4-6. Test series B/C was conducted at LAVA-M while test series D was carried out at LAVA-B. The objectives of the B/C test series were to identify the mechanisms of debris cooling and to understand the effects of various parameters such as debris composition, aspect ratio (melt depth/diameter), and water injection flow rate on the cooling behaviour. Series D was composed of separate effect tests to clarify findings from the B/C series. A total of 21 tests were performed in the two test series.
Each facility was composed of an electric melt furnace (EMF), a concrete vessel, an induction heater, and a water supply system. The corium composition was loaded into the EMF and melted by indirectly heating a graphite crucible with a tantalum inner layer. When the temperature of the melt reached a specified value, the melt was poured into the concrete vessel. Induction heating of the core debris in the concrete vessel was then initiated to simulate decay heat.

The pressure vessel (MR) of LAVA-B was a horizontally oriented cylindrical tank with an ID of 1.8 m, length of 2.5 m, and a wall thickness of 30 mm. The vessel inner surface was thermally insulated to minimise heat losses. The EMF and MR were inerted with argon before each test.

The concrete test sections were cylindrical and made of basalt concrete. The structure and major dimensions of the crucibles are shown in Figure 2.4-7, along with the thermocouple locations. Thermocouples were embedded in the concrete to measure temperatures and to track the ablation front location.
Major parameter ranges for the COTELS tests are summarised in Table 2.4-1. The melt compositions were mixtures of UO\textsubscript{2}, ZrO\textsubscript{2}, Zr and stainless steel. The maximum melt mass was 59 kg. Prototypic core melt compositions for BWR and PWR reactor configurations were defined on the basis of MAAP4 severe accident analyses. For a BWR the corresponding composition was 55/5/25/15 wt\% UO\textsubscript{2}/ZrO\textsubscript{2}/Zr/stainless steel, while for a PWR the composition was 78/17/5 wt\% UO\textsubscript{2}/ZrO\textsubscript{2}/stainless. In test D-6, steel alone was used in order to study the effect of a lower melting temperature. The initial melt temperature for the oxide tests was ~3 000 K. For test D-6 in which steel melt was used, the temperature was 2 170 K.

Table 2.4-1: Parameter ranges for the COTELS experiments

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>Melt Composition</td>
<td>UO\textsubscript{2}, ZrO\textsubscript{2}, Zr, Stainless steel</td>
</tr>
<tr>
<td>Melt Mass</td>
<td>42-59 kg</td>
</tr>
<tr>
<td>Initial melt temperature</td>
<td>2 170-3 350 K</td>
</tr>
<tr>
<td>Concrete type</td>
<td>Basaltic concrete (simulated by quartz concrete)</td>
</tr>
<tr>
<td>Inner diameter of cavity</td>
<td>0.2-0.36 m</td>
</tr>
<tr>
<td>Gross heat input</td>
<td>0-175 kW</td>
</tr>
<tr>
<td>Flooding water flow rate</td>
<td>0-0.2 kg/s</td>
</tr>
</tbody>
</table>

The concrete used in the experiments simulated Japanese LWR basaltic concrete\textsuperscript{6} in terms of aggregate size, distribution, composition, strength, and field curing. As noted earlier, cavity aspect ratio was one of the experimental parameters. Thus, cavity diameters of 20, 26, and 36 cm were tested in the series. The corresponding initial melt pool depths ranged from 7 cm to 22 cm. Gross heat input to the core debris ranged from 50 to 175 kW; the heating efficiency was in the range of 10 to 47%.

Typical results for three experiments are presented here. Test conditions are summarised in Table 2.4-2. Test D-8a is a reference dry cavity case while test D-2 was flooded to examine coolability. Test D-9 was a dry cavity test with the cavity inner wall lined with mortar to examine the effect of aggregate on MCCI progression.

The temperature histories measured in the sidewalls of the concrete crucibles for tests D-2 and D-8a are shown in Figure 2.4-8. When water was injected in test D-2, the side wall temperature gradually decreased following the sharp temperature increase at the beginning of the experiment. As a result, the concrete ablation depth in test D-2 was much smaller in comparison to test D-8a. A cross-sectional view of the crucible for test D-8a is shown in Figure 2.4-9. Post-test examinations confirmed that a layer of degraded concrete including once-molten material formed at the side and below the solidified debris. The degraded concrete layer contained a large amount of un-melted coarse and fine aggregate. It appeared as though the water had migrated into the thermally degraded concrete side wall region for test D-2. The underlying reason for this occurrence is that concrete becomes porous when heated due to the release of concrete decomposition gases, and the corresponding loss of material causes interconnected porosity to form.

\textsuperscript{6} Siliceous aggregates were made nevertheless of crystalline quartz instead of vitreous basalt, increasing the concrete melting temperature.
Table 2.4-2: Conditions for tests D-2, D-8a and D-9

<table>
<thead>
<tr>
<th>Parameter</th>
<th>D-2</th>
<th>D-8a</th>
<th>D-9</th>
</tr>
</thead>
<tbody>
<tr>
<td>Melt Composition (%)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>UO₂</td>
<td>55</td>
<td></td>
<td></td>
</tr>
<tr>
<td>ZrO₂</td>
<td></td>
<td>5</td>
<td></td>
</tr>
<tr>
<td>Zr</td>
<td>25</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>15</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Melt Mass (kg)</td>
<td>48</td>
<td>54</td>
<td>57</td>
</tr>
<tr>
<td>Initial melt temperature (K)</td>
<td>3 120</td>
<td>3 270</td>
<td>2 620</td>
</tr>
<tr>
<td>Concrete type</td>
<td>Basaltic concrete</td>
<td>Basaltic with Mortar layer</td>
<td></td>
</tr>
<tr>
<td>Inner diameter of cavity (m)</td>
<td>0.26</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Gross heat input (kW)</td>
<td>90</td>
<td>75</td>
<td>50</td>
</tr>
<tr>
<td>Initial pressure of Pressure Vessel (MPa)</td>
<td>0.3</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Water injection flow rate</td>
<td>0.03</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Delay of water injection start (min)</td>
<td>9</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

A cross-sectional view of the concrete vessel for test D-9 is shown in Figure 2.4-10. As opposed to test D-8a, a layer of once-molten mortar with un-melted fine aggregate has accumulated at the upper surface of the core debris. This indicates that the molten mortar was pushed out by the heavier, crusted core debris, and this material accumulated as a slag layer on top the debris. This same type of behaviour was observed in the FRAG test series at SNL (Tarbell, Bradley, Blose, Ross, & Gilbert, 1987).

A comparison of the axial ablation depth between D-8a and D-9 tests is shown in Figure 2.4-11. Because of the difference in the net induction heat input to the debris in both tests, the integral heat input to the initial sensible heat was used as the horizontal axis. A larger ablation depth was observed in test D-9 where the cavity inner surface was covered with a mortar layer. The comparison between tests D-8a and D-9 implies that the downward relocation of the solidifying core debris could be dominated by melting of coarse aggregate when the aggregate is thermally more stable than cement. To confirm the hypothesis that the cement and aggregate used in these tests had different melting points, a separate small-scale test was performed in which a concrete sample was heated to 1 623 K. The results indicated the cement had completely melted while the aggregate remained in a solid state.

![Figure 2.4-8: Temperature histories in concrete sidewalls for (a) test D-2 with water injection and (b) test D-8a with a dry cavity](image-url)
**Figure 2.4-9**: Vertical cross section after test D-8a (no water injection, no mortar layer)

**Figure 2.4-10**: Vertical cross section after test D-9 (with mortar layer)
Figure 2.4-11: Axial ablation for tests D-8a and D-9, illustrating the effect of a mortar layer

The relationship between particle bed mass, input power, and ablation depth for the B/C test series is illustrated in Figure 2.4-12 and Figure 2.4-13. There is a tendency for the bed mass to increase with the ablation depth as well as the integrated input power. These trends imply that power input to the melt promotes concrete ablation, and the decomposition gases from the ablated concrete generate particle beds due to melt eruptions. This same phenomenology has been observed in the MACE (Farmer, Kilsdonk, & Aeschlimann, 2009) and NEA MCCI CCI tests (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010).

Figure 2.4-12. Mass of particulate formed before water addition vs. input energy for the B/C test series
Major insights gained from the COTELS project are summarised as follows:

1. No steam explosions occurred in any test when subcooled water was injected onto molten UO₂ – ZrO₂
2. The melt was cooled after water injection and the MCCI was suppressed. This cooling was attributed to water migration into paths formed in the thermally degraded concrete side wall.
3. The test results imply that concrete ablation processes depend on whether or not a stable crust forms at the interface between core melt and concrete.
4. The extent of particle bed formation above the monolithic core debris increases as the ablation depth increases and with the integrated power before water injection. This relationship implies that particle bed formation is driven by decomposition gases released from ablated concrete.

In addition to these findings, the following quantitative conclusions were drawn:

1. The size distribution in the particle beds fits a Rosin-Rammler distribution, and this distribution is independent of the presence or absence of water.
2. Steady state heat removal rates from upper surface of the core debris to overlying water ranged 0.2 to 0.7 MW/m².

2.4.5. **MACE experiments**

As part of an international consortium, a series of coolability experiments were conducted at ANL from 1989 to 2002 as part of the Melt Attack and Coolability Experiments (MACE) Program (Farmer, Kilsdonk, & Aeschlimann, 2009). The principal programme objective was to explore the possible benefits of water addition atop an MCCI insofar as: i) quenching and stabilising core melt, and ii) arresting or even terminating basemat ablation. Four operationally successful integral experiments were conducted; test specifications are provided in Table 2.4-3. Early tests M0 and M1b were conducted with 70% oxidised PWR melt compositions, while the later tests M3b and M4 were conducted with fully oxidised core melts. Three tests in the matrix were conducted with LCS concrete, while the fourth test was conducted with siliceous concrete. A principal parameter in the matrix was test section lateral span, which was varied from 30 cm x 30 cm up to 120 cm x 120 cm. Core melt masses in the various scale tests ranged from 130 to 1950 kg.

The MACE system was very similar to that utilised for the core-concrete interaction experiments performed as part of the NEA MCCI Project (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006).
(Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010); see Section 2.3.9 and Figure 2.3-30 for further details. (In fact, the MACE facility formed the technical basis for the follow-on NEA project). There were, however, several differences in the facilities and these are briefly described below.

**Table 2.4-2: MACE test specifications (Farmer, Kilsdonk, & Aeschlimann, 2009)**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Test</th>
<th>M0</th>
<th>M1b</th>
<th>M3b</th>
<th>M4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Corium type</td>
<td></td>
<td>70% oxidised PWR</td>
<td>70% oxidised PWR</td>
<td>100% Oxidised BWR</td>
<td>100% Oxidised BWR</td>
</tr>
<tr>
<td>Concrete type</td>
<td></td>
<td>LCS</td>
<td>LCS</td>
<td>LCS</td>
<td>Siliceous</td>
</tr>
<tr>
<td>Test section internal dimensions</td>
<td></td>
<td>30 cm x 30 cm</td>
<td>50 cm x 50 cm</td>
<td>120 cm x 120 cm</td>
<td>50 cm x 50 cm</td>
</tr>
<tr>
<td>Sidewall material</td>
<td></td>
<td>Concrete</td>
<td>MgO</td>
<td>MgO</td>
<td>MgO</td>
</tr>
<tr>
<td>Initial melt mass (depth)</td>
<td></td>
<td>130 kg (15 cm)</td>
<td>480 kg (25 cm)</td>
<td>1 950 kg (20 cm)</td>
<td>480 kg (30 cm)</td>
</tr>
<tr>
<td>Initial Melt Temperature</td>
<td></td>
<td>1 730°C (estimate)</td>
<td>1 800°C</td>
<td>1 830°C</td>
<td>1 930°C</td>
</tr>
<tr>
<td>Melt formation technique</td>
<td></td>
<td>DEH</td>
<td>DEH</td>
<td>Chemical reaction</td>
<td>Chemical reaction</td>
</tr>
<tr>
<td>Specific power (actual)</td>
<td></td>
<td>700-1 400 W/kg</td>
<td>350 W/kg</td>
<td>300 W/kg</td>
<td>300 W/kg</td>
</tr>
<tr>
<td>Ablation depth at flooding</td>
<td></td>
<td>1.3</td>
<td>5.0</td>
<td>1.3</td>
<td>3.8</td>
</tr>
<tr>
<td>Water collapsed pool depth</td>
<td></td>
<td>50 cm</td>
<td>50 cm</td>
<td>50 cm</td>
<td>50 cm</td>
</tr>
<tr>
<td>Inlet water flow rate/ temp.</td>
<td></td>
<td>10 lps/20 °C</td>
<td>2 lps/20 °C</td>
<td>2 lps/20 °C</td>
<td>2 lps/20 °C</td>
</tr>
<tr>
<td>DEH power operating mode</td>
<td></td>
<td>Constant voltage</td>
<td>Constant voltage</td>
<td>Constant voltage</td>
<td>Constant voltage</td>
</tr>
</tbody>
</table>

The first difference was in the test sections that contained the core debris. In all four MACE tests, an instrumented concrete basemat was located at the base of the test section. In three of the four tests, the sidewalls of the apparatus were constructed from inert MgO, and so the tests were 1D. However, the M0 test was conducted with concrete walls also, and therefore provided data on 2D erosion characteristics. In the CCI tests, two walls were concrete while the other two walls were MgO.

In the first two experiments M0 and M1b, the corium powder charge was a mixture of crushed UO2 pellets and ZrO2 powders, plus a small amount (typically 8 wt%) of calcined concrete powders (CaO and SiO2). The concrete was incorporated to account for erosion that is expected to occur during the corium spreading phase following breach of the reactor pressure vessel. The melt pool was produced by gradually heating the powders using DEH over a period of several hours. However, for tests M3b and M4 the DEH technique was replaced by specially designed exothermic mixtures that, upon ignition, produced the target core melt composition over a timescale of ~ 30 seconds. This approach was used in all tests conducted in the NEA MCCI Program.

Once the melt was produced and a specified concrete erosion depth was achieved, the cavity was flooded. Water was delivered at a sufficient rate to ensure that the quench process would not be water-starved (i.e. sufficient to sustain a quench rate of at least 10 MW/m2). Once the cavity was flooded, power supply operation was switched from constant power to a constant voltage mode. This was due to the fact that core material that is quenched is not effectively heated using DEH. However, operation in a constant voltage mode maintained the specific power density in the remaining melt zone at the target level. Thus, if significant debris quenching occurred, the input power would decrease while operating in this manner. Steam and non-condensable gases from the interaction were vented through a series of quench tanks and finally an off-gas system. This part of the facility was instrumented to quantify the corium quenching rate (based on the steam condensation rate), as well as the gas composition and flow rate from core-concrete interaction.

A summary of principal experiment findings from the MACE Program is provided below. Data from two of the experiments (i.e. M0 and M3b) are presented in order to illustrate the type of information that was obtained.
The scoping test M0 was performed in order to provide an early indication of the mode and extent of corium quenching, and to provide information that would aid in future experiment designs. Water addition in M0 began when the basemat ablation depth reached 1.3 cm (i.e. at 4.0 minutes; see Figure 2.4-14). At this point, the melt surface camera indicated an agitated pool with no evidence of an upper surface crust. A direct measurement of melt temperature was not available at the time the cavity was flooded due to limited instrumentation (Figure 2.4-14). However, it is believed that the melt temperature was below the Zr melting point (1 857ºC) since an intense interaction was not observed when the Zr rod located at the original core-concrete interface was contacted by melt. Later investigations indicated that the low melt temperature was due to extensive concrete sidewall ablation during the corium heat-up phase before the melt contacted the basemat.

The melt/water heat flux data for M0 is shown in Figure 2.4-15, along with DEH input power normalised by the initial cavity area. The data indicated that during the first three minutes of the interaction, a large cooling transient occurred with the heat flux peaking at nearly 4 MW/m². In the next three minutes, the heat flux fell rapidly and a quiescent period was observed. This occurrence is attributed to the formation of a stable interfacial crust (i.e. the large initial heat removal caused the melt temperature and sparging rate to decline to the point where a crust could form in the presence of the sparging gas). Although perturbations occurred, the heat flux over the next 30 minutes averaged ~ 700 kW/m², which amounts to ~ 70% of the DEH input heat flux of ~ 1 MW/m². After this time the flux diminished steadily to a level of ~ 150 kW/m² at the end of the test.

Figure 2.4-14: MACE M0 Axial Ablation Data (left) and Melt Temperature (right)

Figure 2.4-15: MACE M0 Debris-water heat flux (left) and post-test debris configuration (right)
Post-test examinations indicated that the crust was anchored by the tungsten electrodes. The crust remained at the elevation where it initially formed. As the MCCI progressed downward, an intervening gap formed between the melt and crust, which most likely terminated efficient heat transfer processes between the debris and water. For this particular test, crust-melt separation probably occurred at ~30 minutes into the test sequence, as evidenced by the rapid decline in the heat transfer rate at this time. Despite this, ejections of molten corium through the bridge crust were observed (Figure 2.4-15). These ejections were driven by concrete decomposition gases, which caused pool swelling and entrained melt through the crust into overlying water in the form of dispersed droplets. These droplets quenched as they settled through the water pool and collected on the crust upper surface to form a particle bed (Figure 2.4-15). By the end of the test, the bed depth had reached ~10 cm. The top half was loosely packed while the bottom half was sintered agglomerate. The mass of the bed was ~23 kg, which amounts to ~20% of the initial melt mass. These ejections occurred every 10-15 minutes, and they resulted in transient augmentation in the upwards heat flux to levels in the range of 1-3 MW/m².

During disassembly, the concrete sidewalls were found to be eroded by as much as 10 cm. Sidewall thermocouple data indicated that radial ablation was occurring as early as 50 minutes before the melt reached the basemat. It is estimated that at onset of axial ablation, the total corium mass was ~130 kg, of which 23 wt% was concrete decomposition products from sidewall erosion. This large influx of concrete from the sidewalls during the pre-heat probably depressed the melt temperature to the extent that the Zr was not significantly melted. Thus, the actual M0 test conditions reflect a 100% oxidised PWR melt composition ~23% diluted with concrete oxides run at 2-4 times prototypic decay heat level at 2 hours into the sequence.

Following the scoping test, the facility design was changed to allow larger scale tests of up to 75 cm x 75 cm scale to be performed. The test section was constructed with refractory sidewalls (MgO) rather than concrete to prevent early concrete dilution of the melt. The tungsten electrodes were recessed into the sidewalls to reduce the chances of crust anchoring on these surfaces. A new 0.56 MW power supply was installed and an online gas mass spectrometer was added to the system. Test M1b was subsequently conducted in a 50 cm x 50 cm test section with a collapsed pool depth of 25 cm. This test ran for six hours following onset of ablation. The experiment was terminated on the basis that most basemat thermocouples showed that the concrete temperature had stabilised, indicating that ablation had been arrested. As in the Scoping Test, Test M1b provided evidence of high initial debris cooling rates as well as periodic ejections of molten corium. A large melt eruption was observed early in the sequence that led to a significant transient increase in the upwards heat flux up to ~1.7 MW/m². The heat flux data also indicates that two minor eruptions may have occurred late in the test.

Following M1b, the overall experiment approach for MACE was re-examined. In particular, the facility was redesigned to accommodate the largest possible test section in order to minimise the chances of crust anchoring to the test section sidewalls, as occurred in both previous tests. As shown in Table 2.4-3, this effort resulted in a test section featuring an internal cross section of 120 cm x 120 cm. The corresponding initial melt mass was specified as 1950 kg, yielding an initial collapsed melt depth of 20 cm. The test section design was also upgraded to include a UO2 pellet liner between the corium charge and the MgO sidewalls. The liner was added to eliminate reactions between the corium and the sidewalls, as occurred in M1b. Finally, extensive laboratory testing was carried out to develop an exothermic chemical mixture capable of producing a prototypic core melt composition over a short timescale (i.e. ~30 seconds).

---

7. As CaCO₃ decarbonates, it loses about half of its volume.
These efforts culminated in Test M3b that ran for six hours following onset of ablation. The experiment was terminated on the basis that virtually all melt temperature thermocouples showed that the temperature had fallen below the concrete solidus of ~1100°C (Figure 2.4-16). Water addition began when the ablation depth had reached the target depth of 1.3 cm (i.e. at 52 minutes). Other measurements indicated that the melt temperature at the time water was added was ~1870°C, and the melt sparging rate was ~12 cm/sec.

The melt/water heat flux (Figure 2.4-17) indicated that during the first 20 minutes of the interaction, a large cooling transient occurred with the initial upwards heat flux peaking at nearly 1.8 MW/m². This peak is less than the 4 MW/m² level reached for the M0 and M1b tests. The reduced initial cooling rate was most likely due to the fact the melt temperature for M3b was relatively lower, and also that the corium contained less concrete as an initial constituent (i.e. ~8 wt%) in comparison to M0 and M1b. The lower melt temperature, in conjunction with the reduced presence of concrete oxides, resulted in a more viscous melt which underwent a less effective bulk transient compared to the other tests.

![Figure 2.4-16: MACE M3b axial ablation data (left) and melt temperature (right)](image)

![Figure 2.4-17: MACE M3b debris-water heat flux (left) and post-test debris configuration (right)](image)

Although there are fluctuations, the data generally indicates that the melt temperature did not change significantly following the initial 20 minute cooling transient that resulted in ~1.1 GJ of upwards heat removal. The lack of a bulk temperature decrease indicates that the heat removal was dominated by an interfacial freezing process, as opposed to convective cooling of the entire melt pool. Based on a simple energy balance analysis (Farmer, Kilsdonk, & Aeschlimann, 2009), this amount of
heat removal would have resulted in complete quench of ~ 1 000 kg of core material, which amounts to ~ 50% of the initial melt mass. The post-test debris configuration (Figure 2.4-17 and Figure 2.4-18) revealed the formation of a bridge crust that weighed 1 050 kg, which agrees within 5% of the quenched debris mass predicted on the basis of the energy deposition in the quench system. Other test data (i.e. power supply response) is consistent with quench of ~ 1/2 the initial melt mass during the first 20 minutes of the test. Post-test measurements also indicate that this thick bridge crust was permeable to both gas and water flows. Finally, debris temperature measurements logged during the post-test cool down indicate that water passed through the crust and quenched the underlying debris over the time interval 550-680 minutes after onset of ablation (or 80-210 minutes after the test was terminated). Thus, these various sources of information indicate that a significant cooling transient occurred over the first 20 minutes of M3b which rendered ~ 1/2 the initial melt mass quenched and coolable by virtue of water ingestion into a growing crust.

The crust that formed during the initial interaction also anchored to the sidewalls, eventually leading to the formation of an intervening gap (~ 23 cm) as the MCCI continued downwards. Separation is believed to have occurred at ~ 72 minutes, as evidenced by the steep decline in melt/water heat flux and the resurgence of basemat ablation after this time. Over the next two hours of the experiment, the melt temperature and ablation rate approached steady state conditions, while the melt/water heat flux declined steadily. The reduction in upwards heat transfer was attributed to increasing radiation heat loss to the sidewalls as the melt receded from the suspended crust due to concrete densification upon melting.

As in the previous tests, M3b provided evidence of periodic ejections of molten corium. An eruption was observed on video during the initial stage of the interaction which led to a significant increase in the upwards heat flux to ~ 700 kW/m2. Over the time interval of 252-328 minutes, the input power was increased back to the initial target level of 390 kW. Following the power increase, periodic eruptions were observed until the power was again reduced at 328 minutes. These eruptions resulted in increases in the upwards heat flux to levels as high 700 kW/m2. The post-test debris examinations (Figure 2.4-18) indicated that the ejected material was quenched in the form of a large, centrally located volcanic formation overlying a debris bed which was composed of spherical particles. The mass of the ejected material amounted to 505 kg, of which 310 kg was located in the particle bed, while the balance (i.e. 195 kg) was in lava formations. The 505 kg of ejected material amounts to ~ 26% of the initial melt mass.

After the power reduction at 328 minutes, the melt temperature steadily declined to below the concrete solidus of ~ 1 100ºC at 470 minutes (Figure 2.4-16). This trend is most likely attributable to
the fact that the upwards heat transfer rate approximately balanced the input power during this time (Figure 2.4-17). The test was eventually terminated on the basis that the bulk melt temperature had fallen below the concrete solidus.

Post-test examinations further revealed that ~ 1/3 of the bridge crust failed and relocated downwards at least once. This conclusion is based on the fact that a large fracture ridge was found in the crust which roughly spanned two opposing corners of the test section (Figure 2.4-18). The crust on the side of the ridge that had relocated was highly cracked, and the surface elevations in this area were below the initial collapsed melt height. Thus, although the important objective of maintaining a floating crust was not met in this increased scale test, the M3b post-test debris configuration nonetheless suggests that a floating crust boundary condition is the expected situation at plant scale. This observation was further reinforced by crust strength measurements subsequently made as part of the NEA MCCI Program (Lomperski & Farmer, 2009).

During disassembly, the MgO sidewalls were found to be in very good condition, indicating that the UO2 liner had functioned properly insofar as protecting the test section sidewalls. Thus, during the first 4 hours of M3b, the test conditions reflect a 100% oxidised core melt composition initially diluted with ~ 8% concrete oxides run at prototypic decay heat level at two hours into the accident sequence. During the elevated power period from 252-328 minutes, the power density varied from 2 - 5 times the target level.

Due to the fact that an anchored crust formed in M3b, the decision was made to return to the smaller 50 cm x 50 cm test section. Furthermore, all previous tests were conducted with LCS concrete, and therefore the decision was also made to conduct the final test with a siliceous concrete basemat to round out the test matrix. This test, denoted M4, was conducted with a system very similar to that used for M1b, with the exception that an exothermic chemical mixture was used to generate the melt as opposed to DEH. The mixture was reformulated to contain 8.5 wt% calcined siliceous concrete, as opposed to calcined LCS concrete used for the M1b and M3b mixtures. Test M4 was conducted with a 480 kg corium mass, yielding an initial collapsed melt depth of 30 cm in the 50 cm x 50 cm test section (Table 2.4-3). The test section design retained the use of a UO2 pellet liner to protect the MgO sidewalls. Final important additions to the design were two insertable “crust busters” which were to be used to dislodge a bridge crust should one develop during the test.

M4 ran for 4 hours following onset of ablation, and the test was terminated on the basis that the maximum permissible axial ablation depth of 31 cm had been reached. Water addition began when the ablation depth reached 3.8 cm (i.e. at 22.8 minutes). The melt temperature was ~ 1930 °C at the time of flooding, and the melt sparging rate was ~ 15 cm/sec. Although there was efficient initial cooling of the debris, a bridge crust formed early in the experiment with subsequent gap formation, and this effectively terminated cooling processes. No melt eruptions occurred in this experiment, which may be attributable to the formation of the gap. During disassembly, the MgO sidewalls were found to be in very good condition. Thus, the test conditions for M4 reflected a 100% oxidised core melt composition initially diluted with ~ 8% concrete oxides run at prototypic decay heat level at two hours into the accident sequence.

2.4.6. **NEA MCCI Project CCI experiments**

Aside from examining the thermal-hydraulic aspects of molten core-concrete interaction under dry cavity conditions, a second and equally important aspect of the NEA MCCI test series (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) was to investigate debris coolability under both early and late-phase flooding conditions. The hardware and procedures for these experiments were described previously; see Section 2.3-9.

A total of six tests were conducted as part of the series; specifications and operation summaries for the individual experiments are provided in Table 2.3-9. Of the six tests, four provided quantitative information on the extent of cooling when water contacts molten core debris. In CCI-4 direct melt-
water contact was precluded by the presence of a large mantle crust that formed in the upper regions of the test section due to extensive melt foaming that occurred over the course of the experiment (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006). For CCI-5, the cavity was not flooded due to plugging of the main gas line for the apparatus (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010). Thus, these two tests are omitted from the discussion below.

In terms of phenomenology, four previously identified (Farmer, Kilsdonk, & Aeschlimann, 2009) cooling mechanisms were targeted for investigation at the onset of the MCCI Program: i) bulk cooling, ii) water ingression through cracks/fissures in the solidifying material, iii) melt eruptions, and iv) transient crust breach. As a whole, the test series provided data on all four of these mechanisms. In addition, Test CCI-2 provided data on a fifth cooling mechanism; that is, water ingress at the interface between the core material and concrete sidewalls. In Test CCI-2, thermocouple measurements indicated that water was able to ingress at this interface and effectively cool the concrete sidewalls to saturation, thereby terminating the lateral cavity erosion process. This mechanism had been previously identified in the COTES reactor material test series (Nagasaka, et al., 1999), (Nagasaka, et al., 1999), (Zhdanov, et al., 1999); see Section 2.4.4). Although this is a beneficial cooling mechanism, the results need to be carefully scaled to plant conditions as surface-to-volume effects will diminish the overall effectiveness as scale is increased.

As shown in Figure 2.4-19, the heat flux during the initial flooding stage was generally high for all tests. For the two late-flooded tests conducted with siliceous concrete (CCI-1 and CCI-3), the initial cooling rates approached the Critical Heat Flux (CHF) limitation of ~1 MW/m2 under saturated boiling conditions. Thus, these fluxes were indicative of quenching of the upper surface crust that was initially present for both tests. Although the lance was used to puncture the crust for these tests before the cavity was flooded, the crusts were floating and the openings were generally small compared to the remaining crust surface area over the melt. However, for test CCI-2, the upper surface was essentially crust-free due to the insulating effect of the overlying crust mantles that had formed over the previous five hours of dry cavity operations. Furthermore, for CCI-6 only a thin skin crust was present and the melt was well sparged due to the early nature of the interaction. Thus, for both of these tests bulk cooling transients resulted in which the initial cooling rates were quite high (i.e., ~3 MW/m2 and ~5 MW/m2 for CCI-6). As is evident from Figure 2.4-19, the heat fluxes eventually fell below 1 MW/m2 after ~5 minutes. At this time, stable crusts most likely formed at the melt-water interface, thereby terminating the bulk cooling transient.

![Debris-water heat fluxes for late-flooded tests CCI-1 through -3 (left), and for early flooded test CCI-6](image-url)
In general, the tests did not exhibit a pronounced decrease in overall melt temperature after cavity flooding (Figure 2.4-20). This is despite the fact that the heat flux and power supply responses indicated significant debris cooling. This type of behaviour can be rationalised by a latent heat transfer process in which a quench front develops at the melt/water interface, as opposed to a sensible heat transfer process in which the entire melt mass is cooled by convective heat transfer where the heat is dissipated to the overlying water by conduction across a thin crust at the melt/water interface. Indeed, after incipient crust formation at the melt/water interface, the bulk melt temperature response, power supply response in constant voltage operating mode, and post-test debris morphology are consistent with development of quenched debris zones as opposed to bulk cool down of the entire melt mass by conduction-limited cooling across a thin crust at the melt/water interface. In fact, as shown in Figure 2.4-20, the average melt temperature in test CCI-2 actually increased for a period after water addition, while the debris was undergoing extensive cooling. Simple energy balance analyses coupled with post-test examination results (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) indicate that in order to extract the heat fluxes from the core debris shown in Figure 2.4-19, water-ingression cooling must have occurred in order to rationalise the thicknesses of solidified crust material discovered during post-test examinations. The potential for water to ingress into core debris was also measured as part of the SSWICS counterpart tests conducted as part of the NEA MCCI Program (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010). A photograph of fractured crust material recovered during post-test disassembly of CCI-6 is provided in Figure 2.4-21.

8. After water addition, the power supply was run in a constant voltage mode to maintain the power density in the melt constant; see Section 2.6 for additional discussion regarding power supply operations for these tests.
Aside from the water ingress mechanism, the tests also provided data on the nature and extent of the melt eruption cooling mechanism after cavity flooding. In particular, significant eruptions were observed for test CCI-2 that was conducted with LCS concrete, and for test CCI-6 that featured early cavity flooding. A photograph of erupted material recovered during disassembly of CCI-6 is provided in Figure 2.4-21. In contrast, no spontaneous eruptions were observed after late flooding for the two other tests conducted with siliceous concrete (CCI-1 and CCI-3). As discussed by Bonnet and Seiler (Bonnet & Seiler, 1992), the gas sparging rate during core-concrete interaction is the key parameter influencing the melt entrainment process during eruptions. Thus, the reduced gas content for this concrete type coupled with the fact that the MCCI was flooded in the late stage may have been a key contributor to the lack of eruptions for these two tests.

Sufficient information was gathered during the CCI-2 and CCI-6 tests to evaluate the melt entrainment coefficient after cavity flooding. This information can be used in models for assessing the extent that core debris can be rendered coolable by virtue of top flooding in accident analyses. For the CCI-2 test conducted with LCS concrete the average entrainment coefficient, defined as the ratio of the melt volumetric entrainment rate to the hot gas volumetric flow rate (Bonnet & Seiler, 1992), was found to be ~ 0.11% for CCI-2. This entrainment rate is consistent with that observed in previous MACE integral effect tests carried out with LCS concrete (Farmer, 2005). Conversely, for CCI-6 conducted with siliceous concrete, data analysis indicates that the melt entrainment coefficient averaged 0.04% over the course of the test. Both of these entrainment estimates are within the range of that required to stabilise a core-concrete interaction over a fairly significant range of melt depths (Farmer, 2005).

The entrainment coefficient data for these two tests are significant since the eruptions occurred under a floating crust boundary conditions (as evidenced by the post-test examinations that indicated the absence of a continuous void region below the crust upper surface in both experiments), and while the input power was decreasing, so that the melt zone was not over-powered during the eruption process. Thus, the data upon which the entrainment coefficient is based are deemed to be prototypic. On this basis, the entrainment coefficient may be used to evaluate the effects of the melt eruption cooling mechanism on mitigating the core-concrete interaction under plant accident conditions for the case of LCS concrete over a range of cavity flooding times, and for siliceous concrete under early cavity flooding conditions.
2.5. The Chernobyl accident

During the April 1986 severe accident at Chernobyl unit 4, tens of tonnes of molten corium interacted with the reactor room concrete. Thus, this accident offers the unique opportunity to obtain information on MCCI behaviour under dry cavity conditions at prototypic scale that augments the data provided in the preceding sections.

In an RBMK reactor there is no pressure vessel but rather 1 661 pressure channels, as in a CANDU reactor. The reactor structures (Figure 2.5-1) include a large (14-metre diameter, 2-meter high) lower biological ‘OP’ shield plate that has no structural role, but houses radiological and thermal shielding materials, including serpentinite and Mg3Si2O5(OH)4 (a residue from asbestos waste processing). There are about 300 tonnes of sand between the Л (L) lateral water shield and the cavity walls.

The reactor cavity is rectilinear with rough dimensions of 18.2 m x 23.6 m. The cavity is divided into quadrants by a reinforced-concrete cross (1 m high, 1.4 m wide) that is visible on Figure 2.5-2. At the centre of this cross, there is a 5.3-m high metallic beam assembly connected to the ‘OP’. The concrete floor9 lies at an elevation of +9.7 m. Above the concrete there is a layer of insulation and then a metallic liner at the +10 m elevation. The vertical walls are also protected by a liner and insulation. The concrete is basaltic (i.e. silica-rich) with a thickness of 1.7 m. There are 8 steam bleeder valves which are attached to penetrations (steam dump tubes) in the reactor cavity floor.

Evaluations ((Bogatov, et al., 2007), (Pazukhin, 1994) indicate that debris from the core and the reactor shielding structure collected in the reactor cavity following the destruction of a 105-110° sector of the ‘OP’ in the southeast (SE) quadrant (i.e. between Л (L)47 and И (I)46 in Figure 2.5-2) and the southwest (SW) quadrant (i.e. between Л (L)48 and И (I)47 in Figure 2.5-2). Due to insufficient decay heat removal and heat produced from oxidation reactions, the debris remained molten for several days. The corium flowed through the piping and ablated the walls and floor of the reactor cavity.

Samples were collected, mostly from the upper surface, since it was very difficult to drill within the corium mass. The SE and SW quadrants were covered with corium (total surface area of ~ 170 m², equivalent to the EPR spreading chamber). The corium was predominately a brown ceramic in the SW quadrant, and ii) predominantly a black ceramic in the SE quadrant; see Figure 2.5-3 ((Pazukhin, 1994), (Burakov, 2013). The compositions of these samples (see) are representative of that determined from samples taken throughout the reactor building.

9. The floor of room 305/2 is not a basemat since there are several levels of rooms below this level, as visible in Figure 2.5.1
Figure 2.5-1: Top view of under-reactor room 305/2 (Bogatov, et al., 2007)

Figure 2.5-2: Repartition of corium on the floor of the reactor cavity
Figure 2.5-3: Corium samples (TOP 1-4: brown ceramic from the large vertical flow; BOTTOM 1-4: black ceramic from room 304/3) (Burakov, 2013)

Table 2.5-1: Average elemental composition (wt%) of corium samples. Light elements (mostly oxygen) have been omitted. (Pazukhin, 1994)

<table>
<thead>
<tr>
<th>Type</th>
<th>Al</th>
<th>Mn</th>
<th>Fe</th>
<th>Pb</th>
<th>Cr</th>
<th>Mg</th>
<th>Ni</th>
<th>B</th>
<th>Si</th>
<th>Ca</th>
<th>Zr</th>
<th>Na</th>
<th>U</th>
</tr>
</thead>
<tbody>
<tr>
<td>Black ceramic</td>
<td>4.8</td>
<td>0.3</td>
<td>1.4</td>
<td>0.05</td>
<td>0.2</td>
<td>2.4</td>
<td>0.1</td>
<td>0.04</td>
<td>29.8</td>
<td>3.2</td>
<td>4.2</td>
<td>8.3</td>
<td></td>
</tr>
<tr>
<td>Brown ceramic</td>
<td>3.3</td>
<td>0.5</td>
<td>0.8</td>
<td>-</td>
<td>0.2</td>
<td>5.0</td>
<td>0.3</td>
<td>0.07</td>
<td>32.7</td>
<td>4.3</td>
<td>4.3</td>
<td>-</td>
<td></td>
</tr>
</tbody>
</table>

It should be noted that there is a very low concentration of uranium in both samples (<9 wt%); this is explained by the large amounts of structural materials mixed with the fuel. Table 2.5-2 summarises the inventory of materials that were present in the cavity, as well as estimates of the masses of these materials that were incorporated into the corium based on the chemical analysis results.

‘Vertical lava flows’ occurred through the steam dump tubes. The corium flow stopped without any intervention at the ground floor and did not damage the basemat. More than 40 boreholes were made in the concrete walls and floor to determine the extent of the corium-concrete interaction in the
reactor cavity. Note that the vertical wall between rooms 305/2 and 304/3 was melted through (Figure 2.5-4) leading to what is known as the ‘horizontal lava flow’.

**Table 2.5-2:** Materials present in the reactor cavity at the beginning of corium formation and incorporated during the MCCI (Bogatov, et al., 2007)

<table>
<thead>
<tr>
<th>Material</th>
<th>Amount in Rooms 504/2 and 305/2 after the accident (tonnes)</th>
<th>Incorporated into corium (tonnes)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel (U)</td>
<td>120</td>
<td>90</td>
</tr>
<tr>
<td>Steel</td>
<td>1,300&lt;sup&gt;11&lt;/sup&gt;</td>
<td>&lt; 20&lt;sup&gt;12&lt;/sup&gt;</td>
</tr>
<tr>
<td>Serpentinite mixture</td>
<td>580</td>
<td>160</td>
</tr>
<tr>
<td>Concrete from under the reactor plate</td>
<td>-</td>
<td>130</td>
</tr>
<tr>
<td>Building construction concrete dropped</td>
<td>950</td>
<td>480</td>
</tr>
<tr>
<td>from the vault from upper level marks</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Sand from the vault filling material</td>
<td>300</td>
<td>280</td>
</tr>
<tr>
<td>Zirconium</td>
<td>?</td>
<td>45</td>
</tr>
<tr>
<td>Graphite</td>
<td>750</td>
<td>virtually none</td>
</tr>
</tbody>
</table>

**Figure 2.5-4:** Schematic illustrating vertical corium flows from reactor cavity (305/2)

10. within the boundaries of the reactor space
11. excludes materials from the ‘cross’ component and non-melted components of the reactor bottom
12. 330 t of melt spread within the under-reactor rooms
Figure 2.5-5: Cross-section (along the K axis in Figure 2.5.1) illustrating the cavity ablation profile due to MCCI (dashed lines show the initial floor position).

Figure 2.5-5 presents the estimated shape of the cavity ablation profile due to MCCI along the K axis (see Figure 2.5-1). The ablation is clearly anisotropic with a maximum axial ablation depth of 65 cm and a maximum lateral depth of 2.4 m (ratio >3). Figure 2.5-6 provides a synthesis of the other ten (10) cross-sectional measurements that were made.

In some of the cuts (i.e. from section I to section K-2 m) the corium spread was not complete and there was little or no contact between the corium pool and the lateral walls. Thus, no lateral ablation occurred in these locations. In the next cuts (e.g. from K to K+2.4 m) significant lateral ablation occurred, reaching a maximum ablation depth of 3.5 m, while the vertical ablation was limited to 1.3 m. The lateral/axial heat flux split was thus ~ 2.7 in these regions. Also note that significant lateral ablation occurred in the direction perpendicular to where the south-eastern wall of room 305/2 was melted through. Table 2.5-3 summarises the ablation depths measured at several different locations. The lateral/axial ablation splits range from 2 to 4.

The lateral/axial heat flux splits determined as part of these examinations are noted to be similar to that found in in the CCI and VULCANO 2D reactor material tests conducted with silica-rich concretes (see Section 2.3). Moreover, the tendency for ablation to move along an oblique line has
also been observed (see e.g. Figure 2.5-7) in Chernobyl. This comparison thus indicates that the ablation anisotropy observed in the tests is not an experiment artefact or scaling distortion, but rather a genuine property of core-concrete interaction with silica-rich concretes. However, it should be noted that the corium in this reactor accident formed in the reactor cavity and was not poured in a liquid state as is expected in LWR accident scenarios.

Finally, note from Table 2.5-1 that by the end of the accident, the black ceramic corium had less than 10 wt% of (U,Zr)O2. Such a low concentration would occur in a PWR only after a very large mass of concrete has been ablated, typically after several days of interaction.

Table 2.5-3: Summary of vertical and lateral ablation measurements

<table>
<thead>
<tr>
<th>Cross section</th>
<th>Vertical ablation</th>
<th>Lateral ablation</th>
<th>Ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td>40 cm</td>
<td>&gt;63 cm</td>
<td>&gt; 1.6</td>
</tr>
<tr>
<td>I+1200</td>
<td>1 m</td>
<td>No</td>
<td>-</td>
</tr>
<tr>
<td>I+1650</td>
<td>80 cm</td>
<td>No</td>
<td>-</td>
</tr>
<tr>
<td>K-3000</td>
<td>70 cm</td>
<td>No</td>
<td>-</td>
</tr>
<tr>
<td>K-2000</td>
<td>80 cm</td>
<td>No</td>
<td>-</td>
</tr>
<tr>
<td>K-1000</td>
<td>80 cm</td>
<td>50 cm</td>
<td>0.6</td>
</tr>
<tr>
<td>K</td>
<td>65 cm</td>
<td>240 cm</td>
<td>3.7</td>
</tr>
<tr>
<td>K+2000</td>
<td>110 cm</td>
<td>300 cm</td>
<td>2.7</td>
</tr>
<tr>
<td>K+2400</td>
<td>130 cm</td>
<td>350 cm</td>
<td>2.7</td>
</tr>
<tr>
<td>L-700</td>
<td>60 cm</td>
<td>40 cm</td>
<td>0.7</td>
</tr>
</tbody>
</table>

2.6 Lessons learnt from molten core-concrete interaction experiments

2.6.1 Operational issues

Molten core-concrete interaction experiments conducted as part of the ACE/MCCI (Thompson D. H., Fink, Armstrong, Spencer, & Sehgal, 1992), (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995), MACE (Farmer, Kilsdonk, & Aeschlimann, 2009), and NEA MCCI

13. As the lateral wall was melted through, it is not possible to estimate what would have been the ablation if the wall had been thicker.
Programs (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) used the direct electrical heating (DEH) method to simulate decay heat in the core debris at a power level typical of a PWR at ~2 hours after reactor scram.14 After melt formation, the power was held constant during the dry cavity phase of the experiments. In many tests, the cavity was flooded after a predetermined time or ablation depth was reached to provide additional data on core debris coolability. Following water addition, the power supply operating mode was usually switched from constant power to constant voltage. This was done due to the fact the DEH does not appreciably heat solidified material, and so if debris quenching occurred, then the power density in the remaining melt would increase to non-prototypic levels if constant power operation was maintained. For a given melt resistivity, constant voltage operation preserves power density since the overall melt resistance increases as debris is quenched, and so the electrical current (and thereby gross power) naturally adjusts to maintain the target density.15 However, in reality the melt resistance evolves over the course of the experiment due to changes in composition (by incorporation of concrete slag due to concrete ablation) and temperature (by virtue of debris cooling).16 There are uncertainties associated with estimating the amount of ablation and melt temperature during the experiments, as well as the overall affect that these parameters have on corium resistivity. These uncertainties need to be factored in to the analysis of the test data.

There are generally two criteria used for the termination of an MCCI experiment. The first is that the ablation depth (axial and/or radial) reaches a specified maximum value. For wet cavity tests, the second criterion is that a predetermined time after water addition is met. The maximum ablation depth specification ensures that there is no gross failure of the interaction crucible. The choice of test duration after water addition, on the other hand, is predicated on the notion of the debris cooling rate being reduced to a sufficiently low level such that further continuation of the experiment is not expected to yield additional useful data. Generally, the ablation limit is a more restrictive termination criterion that controls the test duration after water addition. This duration varies from as little as 30 minutes (typically for siliceous concrete) to ~ 2 hours or more (typically for limestone concrete). Extrapolation of limited data from shorter duration tests to much longer duration reactor scale core-concrete interactions may be problematic. Therefore, some degree of conservatism may be prudent in specifying the test termination criteria.

Precise measurement of melt temperature in extremely high temperature environments has always been a challenge in experiments. Even with the significant advances made in thermocouple technology, it is not unusual to witness a large number of thermocouples failing in a given experiment. Moreover, the reliability of data from surviving thermocouples may be questionable, especially since these thermocouples are operated in extremely corrosive environments and the large dimension of their protective sheaths may act as thermal bridges perturbing the temperature field. Consequently, uncertainties in thermocouple data need to be factored into any analysis.

Finally, crust anchoring has been found to have a profound (and detrimental) influence on core debris cooling behaviour that is not expected at reactor scale. To avoid crust anchoring in future

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14. Neglecting noble gases, the power density in the melt would normally correspond to ~300 W/kg fuel at this time for a PWR. The corresponding average heat flux to the surfaces of the melt for a 30 cm collapsed melt depth was in the range of 150-200 kW/m² at this power density level.

15. See Appendix B of reference (Farmer, Kilsdonk, Aeschlimann, & Lomperski, 2010) for a description of the theory underlying constant voltage operation during core debris coolability experiments using the DEH technique.

16. The resistivity of molten core debris increases as concrete decomposition products from ablation are introduced into the melt, and as the melt temperature deceases.
experiments will most likely require a new concept for a test design. For example, in the PERCOLA simulant material test programme (Tourniaire B., Seiler, Bonnet, & Amblard, 2000), a novel method was developed in which a structure simulating an upper crust was counter-balanced in order to create a floating crust condition in a liquid pool. Challenges with this method include developing a technological solution for maintaining a liquid state in the gap around the upper structure, and to avoid any bypass by gases arising from concrete decomposition. An intermediate step could be to design such a new test facility and to verify the concept using a medium temperature simulant fluid before attempting to adopt the method for use in reactor material test facilities.

2.6.2. Material issues

The MCCI experiments conducted over several decades presented many materials issues ranging from the choice of melt composition to the stability of structural materials used in test rigs due to the high temperatures and corrosive nature of core melts. At the same time, these experiments also provided significant learning opportunities for designing and conducting successful large-scale experiments involving prototypic core materials.

Foremost among the material issues surrounding the MCCI experiments is the recognition that the vast majority of tests with prototypic core materials are limited to oxidic compositions. In contrast, it is likely to have a mixed melt composition (i.e. oxide and metal) in the reactor case due to the presence of structural steel incorporated into the melt during the in-vessel phase, as well as the presence of steel reinforcing bar in the concrete basemats of all plants. Thus, when extrapolating test results to reactor cases, there should be some consideration of uncertainties regarding melt composition, particularly as it relates to the presence of structural steel in the melt. The VULCANO programme has conducted a few experiments with mixed metal-oxide compositions (Journeau C., et al., 2009), (Journeau, et al., 2012), but data are limited and additional experiments are needed to fully explore the effect of high metal content on core-concrete interaction behaviour.

From the experimental viewpoint, conducting reactor material experiments with a significant metal phase presents several challenges, a few of which are outlined below.

1. The prototypic metal phase contains zirconium or even uranium (for specific in vessel conditions). Oxidation reactions for these metals are very exothermic, which can significantly reduce the test duration and make the test interpretation more difficult due to the fact that the chemical reaction power is hard to characterise.

2. Under prototypic conditions the decay heat is mainly located in the oxide phase, and this is difficult to reproduce given current limitations on heating technology. In the event that the metal phase stratifies, then a short circuit is created that defeats the direct electrical heating method. Moreover, for facilities that utilise tungsten as the electrode material (such as the ANL test facility), the formation of a low-temperature Fe-W eutectic can lead to rapid erosion of the electrodes. For facilities that utilise the induction heating method (such as the VULCANO facility), the magnetic screening of the lower pool part where the metal is expected to be may not be sufficient because the presence of metal droplets in suspension within the oxide can create a screen that significantly disturbs the power injection in the oxide phase. As a consequence, the power distribution is not known with a high degree of confidence.

3. In high temperature experiments, the design of intrusive probes is always difficult but in metal-oxide systems the probes have to resist both phases, particularly after stratification occurs. Nevertheless, there are indications that the CEA team has developed appropriate technology to overcome these difficulties by selecting specific shielding materials. The thresholds between stratified and homogeneous pools are extrapolated from simulant experiments, and it was not possible to make online measurements during the VULCANO tests. As no appropriate dimensionless analysis has been conducted to justify the choice of the simulant fluid or the form of the correlation to express those thresholds, the design of an
appropriate probe to make measurements in prototypic material experiments should be encouraged.

4. In metal-oxide experiment, it is also difficult to characterise from existing instrumentation the time of change in between stratified and mixed configurations and no specific device has been implemented.

Material compatibility, i.e. potential adverse interactions between melt and other structural components of the test facility, is another issue that caused failures in some early MCCI experiments and provided considerable challenges in designing future experiments. For example, the sidewall failed prematurely in the MACE test M2 (Farmer, Kilsdonk, & Aeschlimann, 2009) due to chemical interaction between siliceous melt and magnesia liner on the sidewall causing a spill of molten material outside the test section and termination of the test. Another MACE test (M3) was also terminated prematurely due to test section over-pressurisation that was caused by excessive hydrogen production from the melt mixture during the chemical reaction stage of the experiment used to produce the core melt. This over-pressurisation was due to moisture ingress into the mixture during pre-test operations. The experimentalists learnt from these failures and challenges and designed future experiments (in both the MACE series as well as the NEA MCCI series (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) with a particular focus on material compatibility issues.

In two recent MCCI experiments, steam line failure has occurred. While these failures did not warrant test interruption or termination, the functionality of the quench system was compromised as a result. Upon flooding, the steam production eventually became excessive and masked the test section because the quench system was not fully functional. Excessive steam also caused moisture ingress into various instrumentation ports (thermocouple junctions, pressure transducers, etc.) and the data acquisition system, resulting in temporary loss of data channels and/or spurious recording of data.

Post-test examination of the failed steam line in the first of the two identified experiments indicated thermal ablation of the flange material (stainless steel). Thus, measures were taken in designing the second experiment whereby the inside surface of the flange was protected with mullite – a ceramic material composed of alumina and silicate. Despite the protective measure, the stainless steel flange in the second experiment experienced thermal ablation – as intense as in the first experiment. Moreover, the post-test examination of the flange revealed no trace of the mullite liner indicating perhaps its dissolution due to chemical interaction. This recent experience suggests that additional care should be exercised in designing the steam line, quench system, and other components of the integral test rig so as not to produce adverse consequences during test operation.

2.6.3. Results of MCCI experiments and their significance

The various test series summarised earlier in this section principally investigated the effects of melt composition, concrete type and input power on two-dimensional core-concrete interaction under both wet and dry cavity conditions. Principal variables measured during the experiments included melt temperature and local concrete ablation rates. For flooded cavity experiments, the debris/water heat flux after cavity flooding was also estimated based on the rate of steam production from the interaction. Key observations from the tests are summarised below.

Under dry cavity conditions, all tests exhibit the overall trend of decreasing melt temperature as ablation progressed, which was due to a heat sink effect as relatively cool concrete slag was introduced into the melt, as well as the increasing heat transfer surface area as the melts expanded into the concrete crucibles. The decline in melt temperature may further reflect the evolution of the pool
boundary freezing temperature that decreased as additional concrete was eroded into the melt during the tests.

The effect of unoxidised Zr cladding on the thermalhydraulics of the core-concrete interaction was investigated in several experimental programmes. The oxidation reaction between Zr and sparging concrete decomposition gases (CO2, H2O) caused exothermic transients in which the melt temperatures increased by roughly 100 ºC over a period of tens of minutes in the experiments. This transient behaviour was observed in both reactor material as well as simulant experiment tests. The data further indicate that after the cladding is fully oxidised melt temperature drops to that consistent with fully oxidised melt conditions.

Aside from cladding oxidation effects, a limited number of experiments have been conducted to examine the effect of significant structural steel content in the melt on core-concrete interaction behaviour. One surprising outcome from these tests is the extensive amount of iron oxidation that occurs with limestone-common sand concrete. Steel oxidation also occurred in tests with siliceous concrete, but to a lesser extent in comparison to the limestone case. The most surprising result from tests with siliceous concrete is that spatial segregation occurred, but it was not completely driven by gravity. In particular, steel layers were found on both horizontal and vertical surfaces. Solidified steel were also found in the oxide. Oxidic melt and unmelted concrete aggregate were also trapped in some parts of the metal layer. Concrete temperatures showed that axial and radial ablation was more pronounced in the areas where metal was found. This is consistent with other oxide-metal simulant tests that have shown enhanced heat transfer at the metal-concrete interface relative to the oxide-concrete interface.

Despite these findings, the metal-oxide database is noted to be quite limited and there is not a clear understanding of the phenomenological behaviour for this case (i.e. mixed vs. stratified metal-oxide conditions, and/or bifurcation between these two states during the interaction). Therefore, extrapolation of results obtained thus far to plant conditions is uncertain. The importance of these uncertainties depends on the reactor design. For example, sensitivity analyses in French PWR900 calculations show that the impact of these uncertainties on the overall melt-through time is manageable. For 3-4 m basemat thickness, the duration of such stratified configurations are very limited in time (less than 10 hours) compared to the duration of ablation (several days) (Atkhen & Spindler, 2010) (Cranga, Michel, Mun, & Barrachin, 2010).

Several experimental programmes have also provided evidence that initial crust formation on cold concrete surfaces can influence the early (tens of minutes to ~hour) core-concrete interaction behaviour. During this phase, basemat ablation is minimal and melt temperature remains high due to the insulating effect of the crusts. This effect has been observed in both transient as well as sustained heating reactor material tests. However, once surface crusts fail ablation proceeds vigorously and the melt temperature declines due to the above mentioned affects. Although the data are not conclusive, there is evidence that gas evolution from concrete decomposition can act to destabilise these interfacial crusts. This is due to the fact that the duration of this crust stability period has been found to be inversely proportional to concrete gas content in several experiments.

Aside from initial crusting effects, the data from several reactor material experimental programmes indicates that the long-term ablation process for oxidic core melts is influenced by concrete type. The results indicate that for high gas content concretes such as limestone-common sand,

17. If the presence of a crust at melt concrete interface is still an open issue, the freezing temperature between melt and upper crust boundary will decrease as a function of concrete fraction whatever the hypothesis is considered between liquidus temperature and temperature corresponding to a given solid fraction close to a mobility threshold usually around 50 to 60%.
the radial/axial power split is ~ 1. Conversely, for low-gas content concretes such as siliceous, this ratio is ~ 3. Forensic examinations at Chernobyl Unit 4 are also consistent with the experiment observations for siliceous concrete. This overall trend in the ablation front progression that has been observed under experiment as well as prototypic conditions cannot be explained on the basis of our current understanding of these types of processes. Thus, extrapolation of the results to plant conditions is currently uncertain due to the lack of a phenomenological model that can rationalise the differences in the observed cavity erosion behaviour.

Post-test examinations in the experimental programmes has shown that the nature of the core-concrete interface is noticeably different for limestone tests in comparison to siliceous; i.e. the ablation front for the siliceous concrete tests consists of a region where the core oxide had locally displaced the cement that bonded the aggregate. Conversely, for tests with limestone concrete, a powdery interface exists in which the core and concrete oxides are clearly separated. The interface characteristics may influence the heat transfer at the interface, yielding different ablation behaviour for the two concrete types.

One question that has been raised is whether the relatively small melt pool aspect ratio (viz., test section width/melt depth) used in the experiments has an influence on the radial/axial power split observed in the dry cavity experiments. A dedicated large scale experiment was carried out to investigate this effect. The results indicated that an increase in aspect ratio from ~ 1 (typical of most reactor material tests) to ~ 3.2 has no noticeable effect on ablation characteristics for siliceous concrete. This observation lends additional credibility to the measured power splits in various experimental programmes. Finally (and as noted previously), the forensics examinations regarding power split from Chernobyl are consistent with the test data for low-gas content siliceous concrete.

Aside from overall cavity erosion behaviour, video data indicates that a crust is typically present over the melt surface during a large fraction of dry cavity ablation. The crusts contain vent openings that allowed eruptions to occur as the tests progressed. The frequency and intensity of the eruptions was directly correlated to the concrete gas content.

In terms of the chemical analyses conducted as part of the test programs, samples were generally collected to: i) characterise the lateral and axial composition variations of the solidified debris, and ii) characterise the composition of corium regions that played key roles in the coolability aspects of the tests (e.g. porous crust zones formed at the melt/water interface, and the material erupted after cavity flooding in CCI-2). Analysis of samples taken to characterise the lateral composition variation indicate that for most tests, the corium in the central region of the debris had a higher concentration of core oxides in comparison to samples collected near the two ablating concrete sidewalls. Conversely, samples taken to characterise the axial composition variation indicated the general trend of slightly increasing core oxide concentration as the concrete surface is approached. For tests conducted with siliceous concrete, two zones were typically present: a heavy monolithic oxide phase immediately over the basemat that was enriched in core oxides, with a second overlying porous, light oxide phase that was enriched in concrete oxides.

Regarding fission product release during MCCI, the composition of aerosols produced has been found to be different from that of aerosols generated under in-vessel conditions, which is likely attributable to the addition of compounds of sodium, potassium, magnesium and calcium arising from concrete erosion. The phenomenology leading to aerosol generation is complex; the mechanisms include evaporation into gas bubbles generated from concrete decomposition (evolved gas species include H2O/H2 and CO2/CO) and at the surface of the corium-concrete mixture, as well as by mechanical generation of droplets due to sparging.

Sustained heating tests conducted with reactor materials and non-radioactive mockups of various fission product species (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995) indicate that aerosols contain mainly constituents of the concrete. In the tests with metal and limestone/sand siliceous
concrete, silicon compounds comprised 50% or more of the aerosol mass. Releases of tellurium and neutron-absorber materials – silver, indium and boron (from boron carbide) – were high. Releases of uranium and low-volatility FP elements were small in all tests. During ablation of the concrete, aerosol composition remained fairly stable and particles were compact but varied considerable in size, the majority being typically micron-sized (geometric diameter) but with some considerably larger sizes.

Regarding debris coolability, the test series have provided evidence of several heat transfer mechanisms that can contribute to long-term corium cooling. When the core debris is flooded from above, the question of whether or not a significant amount of the thermal energy will initially be removed depends upon whether a stable crust is able to form that inhibits heat transfer from the melt to the water layer. For a stable crust to form, two conditions must be met: i) a thermal condition, viz., the melt/water interfacial temperature must fall below the corium freezing temperature, and ii) a mechanical condition, viz., the incipient crust must be stable with respect to local mechanical loads imposed by the agitated melt. If either of these two conditions is violated, then stable crust formation is precluded. In this bulk cooling regime, efficient melt/water heat transfer occurs due to conduction and, predominately, radiation heat transfer across the agitated (i.e. area enhanced) melt/water interface, in addition to entrainment of melt droplets into the overlying water.

As bulk cooling heat transfer continues, the melt temperature gradually declines. As the downward heat transfer rate decreases, then melt sparging arising from concrete decomposition also decreases. Thus, a point will eventually be reached at which the thermal and mechanical thresholds for interfacial crust formation are both satisfied, and an insulating crust forms between the coherent melt zone and water layer. The crust will be characterised by some degree of porosity, or cracks, owing to the necessity of venting concrete decomposition gases.

After the crust forms, completion of the quench process can only be achieved if one of two conditions is met. The first condition is that the melt depth lies below the minimum depth at which decay heat can be removed via conduction heat transfer alone (viz. ~10 cm based on a typical PWR decay heat level two hours after scram). This case is trivial, and is not addressed further. The second condition is that water is able to penetrate into the debris by some mechanism to provide sufficient augmentation to the otherwise conduction-limited heat transfer process to remove the decay heat. The tests have revealed three mechanisms that provide pathways for water to penetrate the debris. The first is water ingression through interconnected porosity or cracks. This process relies on crack propagation through the material and, as such, is highly dependent upon the mechanical properties, since thermal stress is a key factor. The second mechanism is particle bed formation through “volcanic” eruptions. In this case, concrete decomposition gases entrain melt droplets into the overlying coolant as they pass through the crust. The entrained droplets then solidify in the overlying coolant and accumulate as a porous particle bed atop the crust. The third mechanism is mechanical breach of a suspended crust. In particular, the thick crusts that form from water ingestion could bond to the reactor cavity walls, eventually causing the melt to separate from the crust as the MCCI continues downwards. However, this configuration is not expected to be mechanically stable due to the poor mechanical strength of the crust in comparison to the applied loads (i.e. the crust weight itself, plus the weights of the overlying water pool and the accumulating dispersed material). Eventually the suspended crust will fail, leading to rapid ingestion of water beneath the crust. This sudden introduction of water provides a pathway for renewed debris cooling by the bulk cooling, water ingestion, and melt eruption cooling mechanisms.

Several findings related to debris coolability are directly applicable to evaluating plant accident sequences. In terms of the water ingression mechanism, the test results indicate that the heat transfer correlation based on previous one-dimensional (SSWICS) tests is conservative insofar as calculating ingestion-limited crust growth behaviour. In particular, the correlation tends to under-predict the heat flux to overlying water during time intervals when water ingestion is occurring. In terms of the melt eruption mechanism, significant eruptions have been observed in the case of limestone common sand
concrete. Eruptions have also been observed under early cavity flooding conditions for siliceous concrete. For tests conducted in 2D cavity configurations, the eruptions appear to have occurred under a floating crust boundary condition, which is expected at plant scale. Finally, the crust breach data indicate that the crusts that form at the melt/water interface after cavity flooding are quite weak, and will not be mechanically stable at plant scale. Rather, the crust is expected to fail and, thereby, maintain a floating crust boundary condition that will allow the melt eruption and water ingress cooling mechanisms to proceed.

Note that the debris coolability experiment database consists almost exclusively of tests conducted with oxidic melts. As noted earlier, a significant melt metal fraction may be present that may result in a stratified pool configuration. This type of pool structure has not been evaluated from a coolability standpoint. Thus, additional analysis and testing may be required with melts containing a significant metal fraction to further reduce phenomenological uncertainties related to debris coolability, as well as core-concrete interaction.
3. MAJOR MCCI SIMULATION TOOLS AND MODELS

Major simulation tools currently used in the world are described focusing on models currently used to describe the main phenomena involved. Considered codes are by alphabetic order: CORCON, CORQUENCH, COSACO, MEDICIS (GRS), MEDICIS (IRSN), TOLBIAC-ICB, WECHSL, COCO, MAAP, and SOCRAT (MCCI).

3.1. Modelling approach

Why is the lumped parameter approach currently used instead of more recent and possibly more powerful approaches such as CFD, DNS? Answers to this question are multiple and can be listed as follows:

As far as heat convection is concerned, no adequate and validated modelling of multiphase thermal-hydraulics of a pool submitted to gas injection including mass, momentum and energy balance equations is available in a 2D configuration, on the main reasons is that no well-established and validated constitutive laws, determining conditions along boundary pool interfaces in case of gas injection imposed at these interfaces are not available in particular in case of a flow turbulent regime not to speak of the configurations with two immiscible liquid phases; an attempt performed at IRSN to recalculate CLARA simulant experiments on 2D convection driven by gas bubbling remained so far unsuccessful;

The real structure of a pool/concrete interface is only known very approximately and likely to be linked to instability of crusts and boundary layers and depending on the pool scale; a detailed description of these pool interfaces coupled with the pool thermal-hydraulics would be a huge task which was never reached even by very far;

The strong coupling for a corium/concrete mixture of thermal-hydraulics, local corium composition, affecting transport properties and freezing temperatures (Seiler & Froment, 2000), increases further the task complexity;

The outcome of the recently performed R&D (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014) showed that the impact of the structure of pool/concrete interfaces on 2D ablation is prevailing on those of the detailed 2D convection mechanisms within the corium pool;

Last but not least, it is not sure that such a complex modelling is really required to perform reactor predictions, since after at most a few hours, the use of an energy balance equation in a quasi-state regime is reasonable and permits to evaluate the concrete ablation kinetics, provided that the heat flux profile along the pool interfaces can be evaluated using simplified assumptions and models validated against the experimental database.

Therefore the use a lumped parameter approach (Figure 3.1-1) based on the averaging of physical quantities over the whole pool or separated layers is preferred and seems to be both easier and more reliable. Such an approach requires knowing convective heat transfer coefficients for describing heat transfer from bulk pool to interfaces and also between layers and detailed heat transfer mechanisms or at least effective heat transfer coefficients for describing heat transfer across the pool interfaces.
Figure 3.1-1: Application of the lumped parameter approach to a MCCI situation

SOCRAT code is an intermediate case: this code uses a finite element approach for determining 2D heat transfer within the pool but does not calculate the fluid velocity and flow pattern, which is much less ambitious than CFD simulation tools (see Section 3.2.8).

3.2. Overall description of codes

Here are indicated main features for each code. Description of basic assumptions of lumped parameter MCCI codes is further detailed in appendix 7.1. The reader is warned that the description of each code is not exhaustive and only major features and specific models are addressed below.

3.2.1. COCO

Major phenomena that are modelled in the COCO, code developed by NUPEC and JNES are illustrated in Figure 3.2-1. In the COCO code, the behaviour of molten fuel is described by a lumped parameter model (0D model) and the transient heat conduction in concrete wall and basemat is expressed on axisymmetric two-dimensional coordinate system coupled with the release models of free water, bound water and carbon dioxide.

Pool configurations treated

The melt pool is either a homogeneous mixture or stratified layers of oxide and metal. The melt pool may be surrounded by a top crust and melt/concrete-interface crust. There is a slag layer between crust and concrete surface. The existence and thickness of crust are decided by the temperature field and the phase diagram.
Heat transfers

Energy equations for debris temperature transient and heat conduction within concrete structure are solved by the fully implicit finite difference scheme.

Correlations and models have been incorporated for solidification of melt to form crust, water ingression into crust, melt eruption, boiling and radiation heat transfer from debris upper surface.

Figure 3.2-1: Major phenomena modelled in COCO

Erosion algorithm

The ablation velocity is calculated using the latent heat of concrete melting, the heat flux to concrete surface from the melt and the conductive heat flux to concrete inward. The heat flux to concrete surface from the melt is calculated from the convective heat transfer from melt to crust/melt interface and the heat conduction in crust (if there exists) and slag.

Figure 3.2-2: Assumptions for direction of heat conduction and ablation
The ablations are assumed in one-dimensional. The basemat is ablated vertically, and for the sidewall, the direction is radial. The lower sidewall (Region III in Figure 3.2-2) ablation is started after the basemat ablation front reaches its level.

3.2.2. CORCON

CORCON-Mod3 (Bradley, Gardner, Brockmann, & Griffith, 1993) is a mechanistic computer code that describes the core-concrete interaction phenomena relevant to the assessment of containment failure and radionuclide release. The principal components of the CORCON system are the core debris, the concrete, and the atmosphere and surroundings above the debris. The code also treats an overlying coolant pool if one is present. The physical system considered by the code consists of an axisymmetric concrete cavity containing debris in one or more layers. The code calculates heat transfer rates from the debris to the concrete and to the top surface of the debris, as well as the heat transfer between layers. The code also calculates concrete ablation, contingent generation of non-condensable gases from core-concrete interaction, and consequent production of fission products.

![Figure 3.2-3: Example of melt-pool configuration in CORCON code](image1)

![Figure 3.2-4: Cavity surface computation in CORCON code](image2)

CORCON-Mod3 is integrated within the cavity (CAV) package of the integrated system-level code, MELCOR, which models progression of severe accidents (i.e. accidents resulting in severe core damage, possibly melting of the core, leading to release of radioactivity) in nuclear power reactors. By default, the CAV package considers all debris, metallic and oxidic, to be mixed into a single layer of homogeneous melt pool. However, there is option to model multiple layers in a molten pool. Boundary conditions for the top surface of the debris are obtained from an associated control volume in the Control Volume Hydrodynamics (CVH) package, which also serves as a sink for heat and gases released during the interaction. If there is a water pool in the associated volume, it is assumed to overlie the debris. A schematic of the CORCON melt pool configuration is shown in Figure 3.2-3.

The shape of the cross-section of the cavity is defined for computational purposes by a series of “body points.” These are the intersections of a fixed series of rays with the cavity surface as shown in Figure 3.2-4. Given the cavity geometry at the start of a time step, the shape change procedure provides a new cavity shape at the end of the time step. The normal recession rate is used at each body point, as calculated by the concrete ablation model.
3.2.3. **CORIUM-2D**

CORIUM-2D [(Parrozi & Fontana, 2013), (Parozzi, 2010), (Polidoro, 2013)] is a fast running code, developed at RSE, aimed at describing heat transfer phenomena among corium, confinement structures, and coolant under given LWR severe accident conditions or prototypical experiments. The code is based on mass and energy balances; momentum transfer is not taken into consideration, while liquid corium convection is considered, even with some simplifications.

The simulation is performed dynamically, using a modified Euler's predictor-corrector integration method. The model calculates the corium and structures temperature field accounting for solid-to-solid conduction, liquid corium convection, coolant convection and boiling, radiative transfer among dry corium and structures and phase changes in corium and structural materials.

The main key assumptions in the code are:

- Solution of energy equations only, which means to assume:
  - heat exchange driven by solid crusts;
  - corium molten pool almost isothermal because of convection;
  - fluid-dynamic analysis of the liquid pool not reliable because of non-homogeneous properties;
- 2D dynamic analysis (Cartesian or axisymmetric);
- Arbitrary number of cells, not equally-spaced nodalisation;
- Corium, structural material and coolant properties self-calculated as a function of temperature;
- Corium cells fixed-corium mass allowed to expand or collapse (confinement assumed at the bottom and side walls);
- Levelling of corium pool.

The heat transfer model of molten corium convection is based on Fieg's and Kulacki-Goldstein's experimental observations [(Fieg, 1978), (Kulacki & Goldstein, 1972)] on internally heated liquids and it is described by the following relations between Nusselt (Nu) and Rayleigh (Ra) numbers:

For upward facing surfaces

\[ \frac{Ranu}{Nu} = 414.0Ra^{0.216} \]  
\[ \text{Equation 3.2-1} \]

For downward facing surfaces

\[ \frac{Ranu}{Nu} = 12.1Ra^{0.103} \]  
\[ \text{Equation 3.2-2} \]

For lateral surfaces

\[ \frac{Ranu}{Nu} = 111.65Ra^{0.244} \]  
\[ \text{Equation 3.2-3} \]

within the range \(10^7 < Ra < 4 \cdot 10^9\).

Heat transfer through liquid-solid interfaces is calculated accounting pure heat conduction through the related boundary layers. Heat transfer within the liquid bulk is evaluated through a characteristic fluid velocity moduli \(u'\) (natural and forced convection velocity fields assumed to be comparable); this characteristic velocity \(u'\) in a molten corium having pool height \(H\), viscosity \(\mu\) and density \(\rho\), at steady state is:

\[ u' = \frac{Re \mu}{\rho H} = 111.65 \left(\frac{\mu}{\rho H}\right) Pr^{-0.375} Nu^{1.25} \]  
\[ \text{Equation 3.2-4} \]

Re and Pr being the Reynolds and Prandtl numbers.
As regards the liquid levelling, a reasonable estimate of the mean velocity of the transferred mass may be obtained by imposing that all the potential energy is converted into kinetic energy; to account for energy dissipation by viscosity in turbulent regime, a small correction is then introduced.

Corium can be described as a debris bed, i.e. as a pack of solid particles arranged in a volume filled with a motionless gas. The heat transfer mechanisms considered in the code are:

- Thermal conduction through gas;
- Radiant heat transfer between adjoining voids;
- Thermal conduction through solid;
- Thermal conduction through the gas film near the contact surface of two particles, radiant heat transfer between solid surfaces.

To calculate the effective thermal conductivity $k_e$ of the bulk discontinuous material, CORIUM-2D takes into account the contribution of heat transfer through the gaps and the solid material, as:

$$-\frac{k_e \Delta T}{l_p} = q_{\text{gas}} + q_s$$  

$\Delta T$ is the temperature difference between the centres of two neighbouring particles, $q_{\text{gas}}$ and $q_s$ thermal fluxes through the void fraction and solid phase.

The radiative heat transfer is computed by Monte-Carlo method accounting real 3D geometry and inclination of enclosures, and re-iterated to account relocation of molten materials.

A complete library of corium and structural materials (in the oxidic and metallic phases) is available for severe accident analyses of LWR reactors. The code description of system including concrete is limited by the lack of models for the chemistry of corium-concrete interaction (loss of concrete water, decarbonatation, oxidation of metals, etc.). For this reason, only cases implying a small concrete ablation, if compared to the whole corium mass, can be reasonably described (e.g. simulation of reactor cavity walls, thin concrete protective layers, etc.). In this light, as it is likely to have a low influence on the overall system behaviour, no temperature dependence is assumed for the concrete properties.

### 3.2.4. CORQUENCH

CORQUENCH, (Farmer, 2001), (Farmer, 2010), was originally developed to provide a simple, modular model of MCCI behaviour that could readily be adapted to investigate the adequacy of melt/water heat correlations as they were developed. However, it is now being used more broadly for reactor applications.

**Pool configurations treated**

The MCCI model within CORQUENCH can perform either a 1D or simplified 2D ablation calculation. The 2D geometry can be selected to be either cylindrical or rectilinear, with average axial and radial ablation depths calculated. Thus, the code does not perform a detailed calculation of the spatial erosion pattern, but rather simplified geometries are calculated. The melt composition can range from fully metallic to fully oxidic; in all cases, the two phases are assumed to be well mixed (i.e. phase stratification is not modelled).

**Erosion algorithm**

In terms of heat transfer at the melt/concrete interface, a transient concrete decomposition model based on integral thermal boundary layer theory is incorporated (Corradini, 1983). This model was upgraded to account for the effects of transient concrete heat-up with simultaneous crust growth following initial
melt contact with the concrete. Sketches of the physical situations modelled in CORQUENCH are provided in Figure 3.2-5. The inclusion of a concrete dry-out model can be important in evaluating both the early and late phases of a core-concrete interaction. In the early phase, transient crust formation can affect the timing of onset of ablation, while in the late phase of experiments with a limited basemat thickness, heat transfer to underlying concrete can fall to low levels as the decay heat decreases, and so conduction into the concrete behind the ablation front becomes important in determining the overall ablation depth.

Although CORQUENCH has fairly refined concrete decomposition models, they are all one-dimensional; no 2D. The calculated concrete response is based on 1D into the concrete; no 2D heat conduction effects parallel to the ablating concrete surface are considered.

**Heat transfer**

The heat transfer coefficient at the melt/concrete interface can be selected from a variety of user options. The code is capable of calculating transient crust formation, growth and failure at the core concrete interface; this effect is important in the early stages of the core-concrete interaction when crusts can result in an initial incubation period where ablation rates are suppressed. At the melt upper surface, radiant heat transfer to overlying structure is calculated when the cavity is dry. When water is present, a detailed heat transfer analysis is performed involving bulk cooling and follow-on transient crust growth phases; the crust can be treated as impervious to water or coolable by water ingestion. Melt eruptions are also treated.

![Figure 3.2-5: Assumed core/concrete interface structure when slag continuously drains through crust (left) and when slag is retained beneath crust (right)](image)

**Energy balance**

The conservation of energy equation includes the following energy source/sink terms: i) decay heat, ii) mass flux of melt from the failed reactor pressure vessel (RPV), iii) chemical reactions between metallic melt constituents Zr, Cr, Fe (in sequence) and concrete decomposition gases H2O and CO2, iv) condensed phase chemical reactions between Zr and SiO2, v) downward (and sideward for 2D case) heat transfer to concrete, including slag heat sink, and vi) heat transfer to overlying atmosphere (wet or dry).
Code peculiarities

CORQUENCH is a simple code to run and is flexible; all user options can easily be selected through user index options. The code produces text as well as graphical output files so key output results can easily be graphed for evaluation.

3.2.5. COSACO

The MCCI code COSACO (Nie, 2004) has been developed with respect to the MCCI processes foreseen in the core melt stabilisation concept of the EPR™. This concept includes a temporary melt retention phase realised by sacrificial concrete in the reactor pit. During this phase all the corium will be collected, independently of the accident scenario. For its long term stabilisation, the melt is spread on the large surface of a core catcher within a water cooled crucible. To protect this crucible during the corium spreading inside the core catcher its surface is covered with sacrificial concrete that will be eroded.

To model the MCCI process, the COSACO code follows an energy-based approach. The specific modelling of the thermo-chemical phenomena in the melt pool addresses the prevailing behaviour of the considered multicomponent melts. The real solution database COSCHEM is integrated into the modelling logic of COSACO by employing the equilibrium solver ChemApp (CHEMAPP, 1999). The data stored in COSCHEM essentially bases on the real solution database TDBCR99 (Chevalier, 1999), which was developed and validated e.g. in the CIT project of the EU’s fourth framework programme. The MCCI is therefore modelled by a coherent description of the thermo-chemical behaviour of the melt pool and its chemical reactions.

Pool configurations treated

The assumed pool configuration of COSACO is oriented towards the EPR™ reactor pit. An axial symmetric 2D geometry as sketched in Figure 3.2-6(a) is assumed. Other geometric pool configurations, as e.g. the EPR™ core catcher, can be approximately defined via an angular sector with an inert inner disc, as sketched in Figure 3.2-6(b).

The melt pool is typically composed of oxidic and metallic corium. In COSACO, these constituents may either be in a layered mode or fully mixed. In the mixed mode a homogenous melt pool due to the intense mixing by the rising MCCI gases is assumed. On the other hand, due to the mutual immiscibility of the metallic and oxidic melt components, layered melt configurations can also be assumed. For this layered mode, depending on the density of the layers two configurations are distinguished. As long as the amount of ablated concrete oxides in the oxide melt layer is low, the density of the oxidic melt is higher that of the metallic melt. Thus the oxide layer is located at the bottom and covered by the metallic layer. On top of the metal, a light slag layer consisting mainly of light oxides is considered. Later, when the density of the oxidic layer has sufficiently decreased due to concrete addition, the density of the oxidic layer falls below that of the metallic layer. This yields a layer inversion after which the oxide melt is located above the metallic layer. The slag layer from the top is then mixed into the oxide melt.

Due to the gases released during the concrete erosion a certain amount of void establishes in the melt. This yields an increase of the melt volume that corresponds to an increase of the melt layer thickness. The so time dependent upper melt surface is considered in COSACO by the axial discretisation of the lateral wall.
Erosion algorithm

Concrete erosion is determined using a quasi-static approach without any heat conduction in the concrete. To calculate cavity profiles, an axial discretisation of the lateral walls is used. The locally eroded concrete thickness is directly determined by the heat flux from the melt and the decomposition enthalpy. Due to phase segregation, crusts of refractory material can be formed at the boundaries of the oxidic melt layer. These crusts add an additional thermal resistance for the heat transfer to the concrete.

Heat transfers

For oxidic or mixed melt pools, the heat transfer coefficients from the melt bulk to the surface of another layer or to the crust covering the concrete surface are obtained by a correlation based on the BALI tests (Bonnet, 1999). For the radial heat transfer, which was not investigated in the BALI tests, the same correlation is used. Furthermore, if a crust exists at the interface with the concrete or another layer, the effective heat transfer coefficient is determined by a series connection of the individual thermal resistances.

In layered mode, the Kutateladze-Malenkov correlation (Kutateladze & Malenkov, 1978) is used for the heat transfer from the metallic melt layer.

The heat transfer between two melt layers is determined by adding the individual thermal resistances between the layers (connection in series), including that of the crust, if any, see 7.1.5.2.

Energy balance

The energy balance for the corium pool considers the decay heat, the sensible heat of the eroded concrete, the heat transferred to other layers, the radiation from the top surface as well as the enthalpy flow associated to the addition of the decomposed concrete and the exchange of various constituents between layers. In the layered melt configurations, the decay heat is distributed between oxidic and metallic layers. The energy involved in chemical reactions and phase transitions is internally considered by the real solution database.

Code peculiarities

COSACO includes a coherent description of the thermo-chemical behaviour of the melt. Any chemical reactions and phase transitions that occur during the ongoing MCCI are taken into account via the use
of a real solution data base in combination with the free enthalpy minimiser ChemApp (CHEMAPP, 1999).

**3.2.6. MAAP**

The Modular Accident Analysis Program (MAAP) is a widely used severe accident code owned by the Electric Power Research Institute (EPRI). The current released version is MAAP5.02 (MAAP, 2011), and MAAP5.03 is expected to be released in the summer of 2014. MAAP can simulate most all major phenomena that might occur during the course of a severe accident, including the ex-vessel phenomenon of molten core concrete interaction.

The MCCI model in MAAP considers that the reactor cavity contains corium as a homogeneously mixed pool. It assumes material stratification is negligible even when appreciable concrete ablation occurs. The corium pool is modelled as having a molten centre surrounded by crusts at the bottom, along the side, and at the top. The mass and energy of the upper crust are tracked separately from the remaining corium pool. This is done so that the upper crust can be quenched to a temperature significantly lower than the remaining pool.

The model uses a simplified pseudo-binary phase diagram to determine thermal properties and solid fraction within the pool. All constituents in the pool are grouped into two major phases: metals and oxides. In each major phase, the constituents are further divided into sub-groups. Phase diagrams are constructed for pairs of the sub-groups. Chemical reactions during MCCI are modelled assuming instantaneous chemical equilibrium among all the reactants (constituents) in the corium pool at each time step.

The corium pool interacts with several one-dimensional concrete heat sinks. In MAAP5.02 and earlier versions, these heat sinks are the floor, the sidewall, and the upper wall (above the corium pool). In MAAP5.03, the floor and sidewall of a sump in the cavity floor can also be modelled. Therefore, in MAAP5.03 there are potentially 5 heat sinks interacting with the corium pool. If the heat fluxes from the corium pool exceed the amount that can be conducted away from the surfaces of these concrete heat sinks, the concrete is ablated. Ablation rates are proportional to the net heat fluxes deposited onto the surfaces. The ablated concrete releases its constituents in gas and liquid forms. Gas may enter or escape from the corium pool. Liquid is assumed to enter the corium pool immediately. The surface area of the corium pool increases as the sidewall ablation continues.

The top surface of the corium pool can radiate to the upper portion of the vertical concrete wall if the cavity is dry or transfer heat to water if the cavity is flooded.

**Erosion algorithm**

The corium pool is represented as a cylinder with its bottom and side surfaces in contact with concrete. Concrete can be ablated if the heat fluxes from the bottom and side of the corium pool exceed what can be conducted away from the concrete surface. As the ablation of concrete continues, the bottom contact area is expanded, and the side contact area is calculated based on the volume of the corium-concrete slag mixture. In MAAP5.03, more corium pool geometry options are available to users besides the default cylindrical shape. One option is a sophisticated ablation front model which allows evolvement from the original cylindrical shape to an axisymmetric curved shape. The second option is a rectangular corium pool with two or four side surfaces ablating. This feature allows more accurate representation of the corium pool geometry in a BWR Mark-I drywell and certain PWR cavities. The third option allows a sump to be modelled. The shape of the sump can be cylindrical or rectangular. In this case, the corium can contact and ablate the floor and sidewall in the cavity as well as the floor and sidewall in the sump. The contact areas of the ablating surfaces are calculated based on the volume of the corium mixture and the covered fraction of each surface.
The corium coolability models in MAAP include a water ingression model, a melt eruption model, and a three-region corium pool model. In the three-region corium pool model, the corium pool is represented by a particle bed, a solidified upper crust, and a molten pool. The water ingression model has two options. The first option is a parametric model, which relies on user input to account for uncertainties in the heat flux from the upper crust to water. The second option is a mechanistic model in which the heat flux is based on models of crack extension, boiling, and dry-out in the heat generating upper crust. The mechanistic model is now the default option in MAAP5.03.

### 3.2.7. MEDICIS

A lumped parameter approach based on a layer-averaged description is retained in MEDICIS code developed by IRSN and GRS since ten years to be used as MCCI module in ASTEC integral code and COCOSYS code, (Cranga, Fabianelli, Jacq, Barrachin, & Duval, 2005), (Cranga, Mun, Michel, Duval, & Barrachin, 2008), (Duval & Cranga, 2008), (Cranga, Mun, & Marchetto, 2010), (Cranga, Michel, Mun, & Barrachin, 2010), (Guillard, et al., 2010). Moreover for each layer, mass balance equations are written per chemical element and the energy balance equation uses enthalpies of element mixtures, which allow to solve mass and energy balance equations independently of the detailed corium chemistry evolution.

#### Pool configurations treated

The melt pool may be either homogeneous or stratified and may evolve versus time. Four layer types are possible in the present version: an oxide layer, a mixed oxide/metal layer in the case of a homogeneous pool, a metallic layer and the upper crust built-up at the pool upper interface.

![Figure 3.2-7: Node displacement at the ablation front](image)

**Figure 3.2-7:** Node displacement at the ablation front

**Figure 3.2-8:** Sketch of pool configuration and of heat transfer mechanisms described in MEDICIS

#### Erosion algorithm

The cavity geometry is either axisymmetric or is slab-shaped with two non-ablatable walls. The cavity erosion algorithm assumes that the cavity boundary is a succession of truncated cones in case of an axisymmetric geometry or a succession of prisms in case of slab geometry. The 2D profile of the cavity boundary versus time is determined using the local energy conservation at each boundary node and the Stefan’s relation to evaluate the ablation velocity and assuming that the ablation front moves along the bissectrix of the angle made of the current node and the two adjacent nodes (see Figure 3.2-7).
Heat transfers

Heat transfers described by the code (see Figure 3.2-8) are the 2D heat convection described by a convective heat transfer coefficient correlation within each pool layer from bulk pool to the pool interfaces (per default correlation fitted on BALI data (Bonnet, 2000), the convective heat transfer between oxide and metal layers described by a convective heat transfer correlation (Greene’s correlation (Greene & Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988) taking into account the build-up of a crust at the oxide/metal layer interface and the heat transfer close to the pool/concrete interface determined by the pool/concrete interface structure described below.

Energy balance is written as:

\[ \frac{d(mh)}{dt} = P_{\text{decay}} - P_{\text{upwards}} + \frac{dm_{\text{abl}}}{dt} h_{\text{conc}}(T_0) \]  \hspace{1cm} \text{Equation 3.2-6}  

where: \( m \) the mass of the corium pool, \( h \) the corium enthalpy, \( P_{\text{decay}} \) the decay power, \( P_{\text{upwards}} \) the upwards heat loss rate, \( \frac{dm_{\text{abl}}}{dt} \) is the ablated mass rate, \( h_{\text{conc}}(T) \) the enthalpy of ablated concrete at temperature \( T \), \( T_{\text{wall}} \) the ablation temperature and \( T_0 \) the ambient temperature, \( \phi \) is the local heat flux at the corium/concrete interface, \( \int \phi dS \) is the integral of this heat flux along the concrete ablation boundary and \( \Delta h_{\text{abl}} = (h_{\text{conc}}(T_{\text{wall}}) - h_{\text{conc}}(T_0)) \) is the concrete enthalpy variation at ablation. The energy balance equation including metal oxidation reactions is solved in MEDICIS using a consistent set of enthalpy functions for all involved reactants and products.

The upward heat transfer from the upper interface to the surrounding walls is due to radiation. A simplified model takes into account only the upper crust and wall emissivities. A more detailed model available in case of coupling to the ASTEC/CPA containment code takes into account shape factors for the different walls, the possible ablation of concrete walls above the pool and the absorption of the radiative heat flux by aerosols present in the reactor pit using a user’s input absorption length.

A source mass term due to successive corium pourings can be described taking into account the pouring rate of each species and the temperature of the poured corium.

Code peculiarities

The tool flexibility permits an easy addition of model variants and modifications of the dependence of convective heat transfer coefficient and of the pool/concrete interface structure versus the interface orientation.

3.2.8. SOCRAT/HEFEST

HEFEST–EVA code (Highly Efficient Finite Element Solution of Thermal problems for Ex–Vessel stage Analysis) is a module of SOCRAT system code package (Bolshov & Strizhov, 2006), which is used for modelling melt-structures interaction after core meltdown at both in–vessel and ex–vessel stages (Bolshov & Strizhov, 2006), (Moiseenko, Filippov, Drobyshevsky, Strizhov, & Kisselev, 2011). HEFEST–EVA can also function in a stand-alone mode. The numerical procedure is based on finite element method (FEM) solution of 2D energy equation along with material relocation algorithms and chemistry models. The treated configurations are: debris in lower plenum, normally or inversely stratified molten pool in VVER lower head with its heat erosion, normally or inversely stratified molten pool in VVER core catcher, melt in VVER reactor pit causing MCCI.
Pool configurations treated

The calculation domain may have either axisymmetric or planar geometry. Pool can consist of either one or two layers with oxide layer atop or below metallic one. Crust build-up (both upper and sidewall) is simulated by change of properties of particular finite element (FE) when its material has to solidify. Solidified FE is detached from the pool and its composition cannot be changed until it melts again.

Erosion algorithm

Ablation model: thermal erosion is modelled as melt propagation in the composite solid medium (Stefan's problem). The heat conductivity in the melt is taken much greater than in solid (effective conductivity of convection). FEs with solid content in which "melt conditions" (temperature, liquid neighbours, etc.) are satisfied are attached to the pool and the composition of the FE and of the pool changes. Their composition is calculated by averaging the previous composition of the pool and of newly added FEs.

Heat transfers

Nonlinear, transient 2D heat conduction equations are solved with coefficients calculated in accordance with the physical model of the considered phenomena: melting, relocation, chemical interaction etc.

Energy balance

Energy balance in homogeneous medium is kept automatically. The balance is estimated and controlled during all time step calculation procedures: numerical solution, material relocation, chemical reactions. The discrepancies, arising mainly due to different material properties of the components during relocation and melt propagation in non-homogeneous medium are distributed in the melt.

Code peculiarities

HEFEST-EVA is a FEM code for wide range of nonlinear 2D transient thermal problems with changeable internal boundaries. In particular, the configuration of the internal pool boundary is calculated during the pool evolution. The bilinear quadrilateral FEs are used. The results are dumped in HEFEST internal format and in VTK format (readable by open PARAView postprocessor). The MCCI specificities are reflected in the user's adjustment in input data, such as material properties, initial composition, options of melt relocation and arrival procedures, chemistry model options etc. In present the recommendations for choice of specific parameters and typical data sets are being developed based on experimental data used in the code models and typical configurations in MCCI.

3.2.9. TOLBIAC-ICB

The main hypotheses of the phase segregation model (Seiler J.-M., Phase segregation model and molten pool thermalhydraulics during molten core concrete interaction, 1996), (Seiler & Froment, 2000) for an oxide melt interacting with concrete are the following: due to the high liquidus temperature of oxide melts and despite the melting of concrete and the presence of gas issued from concrete decomposition, a solid crust is assumed to deposit at the concrete wall. The species that encrust are assumed to be the most refractory species (mainly UO2 and ZrO2 or to correspond to the pool composition at solidification time). A slow crust growth, a high liquid diffusivity and a small diffusion boundary layer thickness are also assumed to maintain local thermodynamic equilibrium. It must be noted that the decay power in the crust is taken into account; for a transient lasting several days, the crust thickness becomes large and this phenomenon is not negligible.
Figure 3.2-9: Schematic view of the liquid corium pool with convection due to gas bubbling
A crust exists at the interface (which is indicated on the zoom)

With this view, the pool is only composed of liquid and consequently has a low viscosity (Figure 3.2-9). The interface temperature between the liquid pool and the solid crust is equal to the liquidus temperature corresponding to the current composition of the remaining oxide liquid phase. This interface temperature is used to calculate the heat flux between the pool and the concrete. The crust thickness is calculated using a thermal model, with the hypothesis of the succession of steady state regimes. A global thermodynamic equilibrium between the liquid melt and the whole solid crust is not considered, since equilibrium is considered to occur locally, only at the interface (not for the liquid and crusts together). Moreover a large temperature gradient occurs within the crust thickness, and a global equilibrium between liquid and solid would suppose an equilibrium temperature which is difficult to define.

TOLBIAC-ICB uses this approach, which is an ideal model. The main point in this model is that no parametric fitting is used. The drawback is that it gives an overestimation of the melt temperatures for some kinds of concrete; however the concrete ablation has generally a low sensitivity to the melt temperature. Moreover the code user may select an option in the code which deactivates the phase segregation model, for sensitivity studies, with a fitting of the interface temperature between melt and crust.

Pool configurations treated

When the thermodynamic equilibrium provides two immiscible liquids in case of pool containing oxides and metals, a mixing criterion is used to predict the stratification or mixing of these two liquids. Either the BALISE correlation (Tourniaire, Seiler, & Bonnet, 2003) or the Epstein criterion (Epstein M., Petrie, Linehan, Lambert, & Cho, 1981) can be used. The first correlation depends only on the density of the liquids and on the gas superficial velocity. The Epstein criterion also depends on the height of the layers and takes the gas superficial velocity into account through the pool void fraction.

Concerning the physico-chemistry of the stratified melt, two options corresponding to two different situations may be chosen. Following the miscibility gap model proposed by (Seiler, Froment, & Defoort, 2003), both metallic and oxidic liquids can be assumed to be at thermodynamic equilibrium. In such case, the two layers are globally bounded by one refractory crust and have the same boundary temperature. A single crust composition is considered and calculated with GEMINI2 since no crust exists at the interface between the two layers. On the other case, both liquids are assumed to be out of thermodynamic equilibrium. In this latter case, both liquids may be bounded by a different crust: an oxide refractory crust for the oxide pool and a metallic crust in front of the metallic pool. Since the liquidus temperature of the oxide is higher than the liquidus temperature of the metallic pool, an oxide crust is expected to form at the interface between both layers. Thermodynamic calculations are then performed for both liquids in order to determine the crust compositions and the
liquidus temperature related to each layer. Moreover in this case debris of refractory oxides in the metallic layer are considered. Evidence of one scenario or the other is not clear, since MCC experiments using prototypic material with clear stratification do not exist. However the scenario with no equilibrium between the two layers seems to be the more likely. But practically the scenario with equilibrium is usually used with TOLBIAC-ICB, because it is simpler (no interfacial crust, no debris of refractory oxides in the metal layer).

**Erosion algorithm**

Due to the low conductivity of concrete, conduction is not considered in the concrete walls. The concrete walls are divided into individual cells, the size of which is defined by the code user (typically 5 cm for reactor case). The heat flux received by a cell is converted into ablation, and the progressive ablation of the cells defines the shape of the cavity. The concrete walls above the liquid level are also defined with individual cells, and the same ablation algorithm applies, giving the cavity shape. Several concrete layers may be simply defined by changing the physical properties of the cells. However, for long terms simulation a specific model has been implemented, which predicts the end of ablation when the heat flux becomes low, and conduction must be considered (Tourniaire, Spindler, & Guillaumé, 2010).

**Heat transfers**

Taking into account remaining uncertainties related to the power splitting on the pool boundaries, the code user may select several different heat transfer correlations on each boundary (BALI (Bonnet, 2000), Blottner (Blottner, 1979), Kutateladze-Malenkov (Kutateladze & Malenkov, 1978), …). The code user may also simply modify the ratio between the bottom and lateral heat transfers. The recommendation is to use weighting factors, between 2 and 3 for silica-rich concretes, around 1 for limestone-rich concretes, rather than very different correlations, written in terms of different variable groups, since this last solution may lead to unexpected variations of the power split.

A crust generally appears at the upper surface of the pool because of radiative heat transfer towards the reactor cavity or water aspersion. A crust thickness and crust surface temperature are calculated, supposing a steady state regime. The melt to crust heat exchange is governed by the heat transfer coefficient and the interfacial temperature (pool liquidus temperature), and does not depend on the external conditions. The crust surface temperature and crust thickness depend on the external conditions, which may be radiation or heat exchange with a water layer. When there is no water aspersion, the crust density is larger than the melt density (crust composed of refractory materials), and the crust continually forms and sinks: it is then only taken into account for the heat transfer, and no crust increase is taken into account. In case of water aspersion, crust porosity is supposed, and the crust increase is taken into account in the model.

For the radiation above the melt surface, two levels of modelling exist in TOLBIAC-ICB. The most simple radiation model only considers a radiation between two infinite planes, the upper crust and the surroundings, depending on the surroundings temperature and the emissivity of the pool surface and of surroundings. A more complex model also exists. The cavity above the melt level is described, shape factors are considered in the radiation model, and ablation of the concrete vertical walls above the melt level is taken into account. The ablation of a horizontal steel wall representing the vessel structure above the melt may also be modelled. Heat losses through gas or aerosols escaping from the melt pool to the containment may be taken into account through a coefficient of power loss defined by the code user.
Energy balance

The steady state energy balance equation reads in case of a homogeneous melt as follows:

\[
S_f h_f (T_{\text{melt}} - T_{\text{int}}) + S_l h_l (T_{\text{melt}} - T_{\text{int}}) + S_{\text{inert}} h_{\text{inert}} (T_{\text{melt}} - T_{\text{inert}}) + S_u h_u (T_{\text{melt}} - T_u) + \dot{q}_s C_p s (T_{\text{melt}} - T_s) = \dot{P}_{\text{res}} + P_{\text{ini}}
\]

Input Power  Initial overheat

This equation is used to calculate the melt temperature \( T_{\text{melt}} \).

\( S \) stands for the heat transfer area with the bottom wall (index \( f \)), with the lateral concrete wall (index \( l \)), with the lateral inert wall (index \( \text{inert} \)), and with the upper surface (index \( u \)).

\( h \) stands for the heat transfer coefficient with the bottom wall (index \( f \)), with the lateral concrete wall (index \( l \)), with the lateral inert wall (index \( \text{inert} \)), and with the upper surface (index \( u \)).

\( T_{\text{int}} \) is the interfacial temperature between the melt and the crust. \( T_{\text{inert}} \) is the interfacial temperature between the melt and the inert wall. \( T_u \) is the interfacial temperature between the melt and the upper surface.

At the upper surface, if there is a crust, the interfacial temperature is the same as for the interfacial temperature between the melt and the crust for the heat transfer with the walls. If there is no upper crust the surface temperature is calculated through a heat balance at the upper interface. Concerning the source term (corium from the pressure vessel at the beginning, and then metals from the pressure vessel melting), \( \dot{q}_s \) is the source mass flow rate added to the melt during time step \( dt \), \( C_p s \) is the heat capacity of this mass, and \( T_s \) is the temperature of this mass. For short transients and especially for the simulation of experiments with a short duration, a transient term is included, which writes: \( M_{\text{melt}} C_{p_{\text{melt}}} (T_{\text{melt}} - T_{\text{ini}} - 1_{\text{melt}})/dt \).

3.2.10. WECHSL

WECHSL-Mod 3 was released in 1995 (Foit, Reimann, Adroguer, Cenerino, & Stiefel, 1995) and further improvements are under development as new experimental or theoretical insights becomes available. The outline of the WECHSL modelling is described below as an example for the requirements and capabilities of a MCCI code.

The WECHSL code aims at modelling the physical and chemical phenomena governing the molten core-concrete interaction in a severe reactor accident, when the molten core has penetrated the pressure vessel. WECHSL models one-dimensional as well as two-dimensional melt-concrete interactions in axis-symmetric concrete cavities. Main implemented models are documented in (Foit, Reimann, Adroguer, Cenerino, & Stiefel, 1995) and (Foit J. J., 1997): The left side of Figure 3.2-10 shows models for a homogeneous oxide melt into which the metal phase is homogeneously dispersed in form of droplets, whereas the right side of the same figure shows the layered melt configuration with the metal layer at the bottom, overlaid by the oxide corium layer in the initially cylindrical concrete cavity.
To calculate one-dimensional experiments, the horizontal concrete may contain an additional layer composed of concrete and metals to take into account those configurations that were used in large-scale ACE experiments (Thompson, Farmer, Fink, Armstrong, & Spencer, 1997). The metallic melt may contain Zr, Cr, Fe, Ni and Si. The oxide layer is composed of UO$_2$, ZrO$_2$, CaO, SiO$_2$, Al$_2$O$_3$, Cr$_2$O$_3$ and FeO. Internal energy can be produced by decay heat or by exothermic chemical reactions. The mass and composition of the melt change as liquid concrete is mixed into the oxide corium melt, and by the chemical oxidation of some of the metals. Energy is lost to the melting concrete and to the upper containment by thermal radiation or by evaporation of sump water, possibly flooding the surface of the melt.

The code performs calculations from the time of initial contact of a hot molten pool until long term basemat erosion over several days with the possibility of basemat penetration.

3.2.11. Synthesis on main features of the MCCI codes

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<th>SOCRAT</th>
<th>TOLBIAC-ICB</th>
<th>WECHSL</th>
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<td>Correlation from CFD calculation results</td>
<td>Slag layer</td>
<td>Fieg's modified and Kutateladze-Goldstein's correlation modified for non-stagnant pool (Fieg, 1978); (Kutateladze &amp; Malenkov, 1978)</td>
<td>BALI (oxide) (Bonnet, 1999) Kutateladze (metal) (Kutateladze &amp; Malenkov, 1978)</td>
<td>Crust absent: Slag layer, gas film, or Savon correlation; Crust present: (Kutateladze &amp; Malenkov, 1978)</td>
<td>User User provided H.T. coefficient or (Mayinger, Jahn, Reineke, &amp; Steinbrenner, 1976)</td>
<td>BALI (Bonnet, 2000) Correlation from exp. Results (GRS version)</td>
<td>Effective thermal conductivity Correlation: CFD calculations results (cylindrical molten pool)</td>
<td>BALI (Bonnet, 2000)</td>
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<td>Fieg's modified and Kutateladze-Goldstein's correlation modified for non-stagnant pool (Fieg, 1978); (Kutateladze &amp; Goldstein, 1972)</td>
<td>BALI (oxide) (Bonnet, 1999) Kutateladze (metal) (Kutateladze &amp; Malenkov, 1978)</td>
<td>Crust absent: Slag layer, gas film, or Savon correlation; Crust present: (Kutateladze &amp; Malenkov, 1978)</td>
<td>User provided H.T. coefficient or (Mayinger, Jahn, Reineke, &amp; Steinbrenner, 1976)</td>
<td>BALI (Bonnet, 2000) Correlation from exp. Results (GRS version)</td>
<td>Correlation: CFD calculations results</td>
<td>BALI (Bonnet, 2000)</td>
<td>Depending on the existing gas flow</td>
</tr>
<tr>
<td>Convective heat transfer at pool upper interface</td>
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<td>Fieg's modified and Kutateladze-Goldstein's correlation modified for non-stagnant pool (Fieg, 1978); (Kutateladze &amp; Goldstein, 1972)</td>
<td>BALI (oxide) (Bonnet, 1999) Kutateladze (metal) (Kutateladze &amp; Malenkov, 1978)</td>
<td>Kutateladze (Kutateladze &amp; Malenkov, 1978)</td>
<td>User User provided H.T. coefficient or (Mayinger, Jahn, Reineke, &amp; Steinbrenner, 1976)</td>
<td>BALI (Bonnet, 2000) Correlation from exp. Results (GRS version)</td>
<td>Correlation: CFD calculations results</td>
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<td>Crust absent; Slag layer; gas film; or Savon correlation; Crust present: (Kutateladze &amp; Malenkov, 1978)</td>
<td>User User provided H.T. coefficient or (Mayinger, Jahn, Reineke, &amp; Stainbrener, 1975)</td>
<td>BALI (Bonnet, 2000) Correlation from exp. Results (GRS version)</td>
<td>Effective thermal conductivity Correlation: CFD calculations results (cylindrical molten pool)</td>
<td>BALI (Bonnet, 2000)</td>
<td>Depending on the existing gas flow</td>
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<tr>
<td>Convective heat transfer at concrete/pool bottom</td>
<td>Correlation from CFD calculation results</td>
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<td>BALI (oxide) (Bonnet, 1999); Kutateladze (metal) (Kutateladze &amp; Malenkov, 1978)</td>
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<td>Correlation: CFD calculations results</td>
<td>BALI (Bonnet, 2000)</td>
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<td>BALI + Kutateladze in series</td>
<td>Not used (no stratification model)</td>
<td>Not used</td>
<td>Greene (Greene &amp; Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988) + oxide crust thermal resistance</td>
<td>HEFEST model</td>
<td>BALI (Bonnet, 2000)</td>
<td>(Weir, 1982)</td>
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NEA/CSNI/R(2016)15

STATE-OF-THE-ART REPORT ON MOLTEN CORIUM CONCRETE INTERACTION AND EX-VESSEL MOLTEN CORE COOLABILITY, NEA No. 7392, © OECD 2017

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<td>Lister-Epstein model (Lister, 1974) (Epstein, 2006), or parametric model</td>
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<td>Options: user-specified entrainment coefficient, 2) Ricou-Spalding model (Ricou &amp; Spalding, 1961), or 3) Farmer model (Farmer, Phenomenological Modeling of the Melt Eruption Cooling Mechanism during Molten Corium Concrete Interaction, 2008)</td>
<td>Ricou-Spalding model with a single coefficient (Ricou &amp; Spalding, 1961). Default value of the coefficient is E=0.08. Ejected corium is modelled as a particle bed above the upper crust.</td>
<td>in ASTEC V1.2: Spalding correlation (Ricou &amp; Spalding, 1961) with parameter E = 0.08 Ejected corium debris mixed with upper crust. In ASTEC V2: layer of debris above the upper crust. Detailed mechanistic model for hole geometry</td>
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3.3. **Comparison and synthesis of code modelling**

The purpose of this section is to compare available models in different codes and to point out limitations of these models.

### 3.3.1. **Thermal-hydraulics models**

Main heat transfer models to be considered are the following:

- 2D heat convection;
- Heat transfer between oxide and metal layers;
- Oxide/metal pool configuration evolution.

They are addressed hereafter.

#### 3.3.1.1. 2D heat convection

The main convection mechanism is gas-driven convection; correlations obtained by Kutateladze (Kutateladze & Malenkov, 1978) or derived from BALI experiments (Bonnet, 2000), or still from Deckwer’s surface renewal model (Deckwer, 1980), (Tourniaire & Varo, 2008) are most frequently used in codes; the value of heat transfer coefficient is typically 100 up to a few 1 000 W/m/K versus viscosity and superficial gas velocity. Free convection and still much more solutal convection (which should be more efficient than free convection) which are not taken into account in codes should increase further the convective heat transfer coefficients. In a previous analysis (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014), it was shown that the convective heat transfer coefficient due to gas bubbling is significantly higher than the effective heat transfer coefficient in oxide melts deduced from experiments. In the frame of thermal resistance approach at the pool/concrete interfaces, the description of 2D convection in the bulk pool using only the gas driven convective heat transfer is a reasonable approximation, this is the case of MEDICIS IRSN code if using a “no-crust “model because of the prevailing thermal resistance of pool/concrete interfaces; however if a stable crust model is used with a pool/crust temperature imposed by thermochemistry, the heat flux distribution in the oxidic pool depends on the level of convective heat transfer coefficient and on its distribution along the pool interfaces.

Experimental data on 2D convection due to gas bubbling were obtained recently in the frame of the CLARA simulant programme (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014). And two sets of correlation have been proposed from these results ( (Michel, 2015) and (Bottin, et al., 2016).

#### 3.3.1.2. Oxide/metal heat transfer

The oxide/metal interface in case of pool stratification has very likely a different structure and involves different heat transfer mechanisms compared to those of the pool/concrete interface, since the gas bubbling will promote entrainment of tiny metal droplets into the upper oxide layer and intense heat transfer from the entrained metal to the oxidic phase.

Available correlations on oxide/metal convective heat transfer coefficient are discussed in detail in (Cranga, et al., 2010). One of the first correlations established for the heat transfer coefficient at a liquid/liquid interface was originally proposed by Werle and relied on simulant experiments (Werle, 1982) performed only at a low gas velocity (<0.7cm/s). The formulation is based on a free convection heat transfer correlation combined with an enhancement factor depending on the superficial gas velocity.

The most frequently used heat transfer correlation (Greene & Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988) is a semi-
empirical one assuming that the interfacial heat transfer process is a transient renewal process on both sides of the interface and that the two resistances to heat transfer can be considered in series. In Greene’s correlation, the heat transfer coefficient does not depend anymore on viscosity whereas experimental results obtained by Greene do. Nevertheless this correlation is often used in MCCI codes in lack of a better one.

Due to limitations of existing correlations on oxide/metal convective heat transfer, a work was launched at IRSN to build an improved correlation taking into account the existing database. This new correlation was fitted to experimental data points reported by Greene and obtained in the ABI test series (Cranga, et al., State Of the Art Report on MCCI in dry conditions: analysis of experiments and modelling, 2013). The analysis of ABI results showed that an approach taking into account thermal resistances in series on both sides of the oxide/metal interface is not appropriate. Therefore a new correlation (Cranga, et al., State Of the Art Report on MCCI in dry conditions: analysis of experiments and modelling, 2013), aimed at describing the global heat transfer across the interface, was finally selected:

\[
\begin{align*}
    h &= 13.8 \Pr_{\text{low}}^{1.65} \Pr_{\text{up}}^{-0.67} \Re_{\text{low}}^{2.37} \Re_{\text{up}}^{-0.35} \left( \frac{U_T^{\text{low}}}{J_g} \right) \left( \frac{U_T^{\text{up}}}{J_g} \right)^{-1.64} \\
    \text{Equation 3.3-1}
\end{align*}
\]

where the subscripts up and low refer respectively to the upper and lower layers and \( U_T \) is the bubble terminal velocity.

\[\text{Figure 3.3-1: Evolution of heat transfer coefficient versus superficial gas velocity } J_g \text{ in the reactor case: Comparison between Greene’s correlation and new correlation versus corium viscosity}\]

An application of such a correlation was made to the reactor case by IRSN (Cranga, et al., State Of the Art Report on MCCI in dry conditions: analysis of experiments and modelling, 2013). Material properties were chosen in order to be representative of the reactor case with a wide range of oxide viscosity to take into account the high viscosity increase in the long term phase due to the high silica fraction caused by the large concrete ablation. These results are compared to those obtained with Greene’s correlation and plotted in Figure 3.3-1.1

1. It can be noticed that the heat transfer coefficient given by the new correlation decreases from \( \mu_{\text{up}} = 0.005 \) to 0.5 Pa.s but increases from \( \mu_{\text{up}} = 0.5 \) to 1 Pa.s. It is due to the fact that the terminal bubble velocity depends also on the viscosity: this velocity decreases with the viscosity and the heat transfer coefficient decreases with the terminal bubble velocity of the upper layer so it has the opposite effect of the explicit viscosity of the upper layer which intervenes in the Prandtl and Reynolds numbers.
With the properties chosen for a reactor case allowing a possible stratified configuration, the heat transfer coefficient is higher with the new proposed correlation by a factor 2 to 5 than with that currently used Greene's correlation. This work confirms also that the heat transfer at the interface oxide/metal is very high compared to the convective heat transfer from the bulk oxidic pool to other interfaces (see 3.3.1.1), if these results can be scaled up to the materials in the reactor case. The possible presence of an oxidic crust at the oxide/metal interface will reduce heat transfer through this interface but in a moderate way.

3.3.1.3. Pool configuration evolution model

There are mainly three experiments with simulant material and associated models available to study the stratification criteria: Epstein (Epstein M., Petrie, Linehan, Lambert, & Cho, 1981), Casas and Corradini (Casas & Corradini, 1992) and BALISE tests (Tourniaire, Seiler, & Bonnet, 2003).

All these studies show the existence of three main configurations which can be observed by increasing (or decreasing) the gas flow rate. For low gas velocity, the pool is stratified. When the gas flow rate is increased, a significant part of the heavier phase is entrained in the light phase. This corresponds to the onset of entrainment ($J_{goe}$) on Figure 3.3-2. When the gas superficial velocity increases further, all the heavy phase is entrained in the lighter part of the pool; this corresponds to the full mixing ($J_{gfm}$).

![Figure 3.3-2: Mixing rate % versus gas superficial velocity in a BALISE test](image)

All the authors proposed a criterion for the superficial gas velocity for the onset of entrainment ($J_{goe}$) and for the full mixing ($J_{gfm}$). Among these criteria, the BALISE criterion appears as an upper bound for the threshold of the stratified configuration. This criterion is based on the ratio between the density of the light phase ($\rho_L$) and the heavy liquid phase ($\rho_H$).

$$J_{gfm} = 0.054(\rho_L - \rho_i)/\rho_L m/s \quad J_{goe} = 0.5J_{gfm} \quad \text{Equation 3.3-2}$$

Several limitations on this modelling appear:

1. Obviously, all variations in the physical properties observed in an assumed MCCI with metal and oxide have not been investigated experimentally. Furthermore, the effect of thickness of the heavy layer and the effect of a 2D injection have not been investigated either. Furthermore, this correlation, based on gas injection from the bottom only, assumes that the whole gas rate flows through the pool and requires an extension to the case of 2D gas injection and a clarified definition of superficial gas velocity used: it is to be considered that either the superficial gas velocity is defined as the ratio of the gas volume rate to the area of the interface (bottom + lateral) or the superficial gas velocity is defined as the ratio of the produced gas volume rate to the pool horizontal section.
The few MCCI experiments with prototypic corium and steel have shown that the configurations may be more complex that the two academic cases previously considered. For instance, in the three VULCANO tests for which a significant mass of metal has been found after the end of the experiment, the metallic mass was not found as a horizontal layer, but had a more complex shape, with metallic phases vertically facing the walls. This shows that phenomena might be far more complex that those represented in simulant material stratification experiments (Tourniaire, Seiler, & Bonnet, 2003) where only two liquid compositions are present. During MCCI, miscible but yet unmixed oxides are present and coexist with immiscible molten metals.

Application to the reactor case

In the reactor case, when MCCI starts, the heat fluxes are high and thus the superficial gas velocity are so high that the “full mixing” configuration may be assumed. One of the reactor case specificity is the decrease of the heat flux on the vertical and horizontal wall during the MCCI. This decrease is due to the increase of the area bounding the pool and to the decrease of the power in the pool as decay heat falls with time. The decrease of the heat flux induces a decrease in the gas flow rate bubbling in the pool. If the gas flow rate falls below the threshold for full mixing, the metal starts to slump and settles at the bottom of the pool. But as the metal heat transfer coefficient is higher than the oxide one, the heat flux to the bottom becomes higher and the gas flow rate increases and the stratification disappears. But as soon the gas flow rate reduces, stratification of the metal starts again. The stratified configuration is really stable only when the power transferred to the bottom wall corresponds to a gas superficial velocity lower than the threshold for the onset of entrainment. Most MCCI codes use specific stratification models or user’s input data. Only TOLBIAC-ICB and MEDICIS use a model derived from simulant experimental data. These codes use two thresholds depending on the current configuration (see Figure 3.3-3). When the current configuration is fully mixed, configuration is kept mixed until the superficial gas velocity becomes lower than a transition criterion, called $J_{g\text{ stableS}}$ on Figure 3.3-3. When the configuration is a stratified configuration, the superficial gas velocity must be higher than a transition criterion, called $J_{g\text{ stableM}}$ on Figure 3.3-3, for a change to a “stable” fully mixed configuration.

![Figure 3.3-3: Flow map in a MCCI code](image)

The criterion derived from BALISE experiments uses the entrainment onset threshold $J_{g\text{oe}}$ for $J_{g\text{ stableS}}$, below which the stratified configuration is stable and the full mixing threshold $J_{g\text{fm}}$ for $J_{g\text{ stableM}}$, above which the homogeneous configuration is stable. However, to avoid oscillations between stratification and full mixing, MCCI codes raise the $J_{g\text{ stableM}}$ to make the stratified configuration more...
stable, which promotes the axial ablation. The occurrence of these oscillations shows that the pool configuration model is probably too crude and some intermediate configuration types, which cannot be described with the present model, need to be included. These oscillations may be physical as stratification enhances heat transfer through the steel layer to the bottom concrete and this increases gas sparging, promoting de-stratification. A minimum metal layer thickness is required to maintain oxide/metal stratification. It is chosen usually between 1 cm and 5 cm: the lower boundary corresponds roughly to the size of a bubble and the upper boundary to the maximum thickness of the possible solid crust built at the interface between the metal layer and concrete. For a layer thickness above the lower threshold value the metal layer has some chance to be separated from the oxidic layer in presence of bubbling and above the upper threshold value the metal layer should be at least partially liquid thus permitting heat convection with the upper oxide layer.

A parametrical reactor study was performed with the ASTEC/MEDICIS code on the impact of pool configuration evolution criteria (Cranga, Mun, & Marchetto, 2010) It highlighted the strong increase of the melt-through time with the decrease of the superficial gas velocity threshold below which stratification appears and a strong dependence on the minimal metal thickness required for pool stratification.

Thus, BALISE simulant experiments have established a convenient criterion for the pool stratification onset. However several uncertainties remain on this stratification criterion: lack of a dimensionless formulation, validation in 2D gas injection conditions, too high a conservatism particularly in the prototypical case of a low ratio of metal to oxide volumes. And, above all, understanding of intermediate configurations (partially stratified and partially mixed) has to be improved.

3.3.1.4. Synthesis on thermal-hydraulics models

Features and limitations of available models on pool thermal-hydraulics are summed-up for each type of phenomena on Table 3.3-1.

Following remarks can be made of sub-items of Table 3.3-1:

2D heat convection: two sets of consistent and validated correlations for describing 2D heat convection have been obtained from CLARA experimental results (Cranga, Spengler, et al. 2013) and depending on corium transport properties and superficial gas velocity profile along the pool/concrete interface (Michel, 2015) and (Bottin, et al., 2016). These sets will contribute to reduce the impact of uncertainties on 2D convection.

Oxide/metal heat transfer: Greene’s correlation gives a heat transfer coefficient value comparable to experimental data or slightly lower; since the obtained heat transfer coefficient is much higher than the lateral convective heat transfer coefficient according to experimental data, the use of Greene’s correlation can be considered as enough conservative because it leads to a focusing of decay power in the oxide layer towards the lower interface with the metal layer, thus promoting the axial ablation kinetics.

Criteria of oxide/metal pool configuration evolution: in spite of uncertainties, BALISE type stratification criterion \( J_g < b_{HS} \Delta \rho / \rho_{\text{min}} \) combined with a minimum metal thickness \( \text{thm} \) is likely conservative if \( b_{HS} > 0.027 \) and \( \text{thm} \) equal to at most 3 cm because it will promote stratification even in situations where a stable stratification may be doubtful.

2. \( b_{HS} \) is the BALISE stratification coefficient for the switch from homogeneous to stratified configuration. Another coefficient \( b_{SH} \) has been experimentally determined for the switch from stratified to homogeneous configuration (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014).
Table 3.3-1: Status on thermal-hydraulics modelling

<table>
<thead>
<tr>
<th>Phenomenon</th>
<th>Codes including its description</th>
<th>Established knowledge/retained assumptions</th>
<th>Limitations</th>
<th>Impact on MCCI predictions</th>
</tr>
</thead>
<tbody>
<tr>
<td>2D heat convection (h_{conv})</td>
<td>All codes</td>
<td>Gas-driven convection is likely to be prevailing in all codes (excepted for SOCRAT using free convection), existing correlations (Bali (Bonnet, 2000), Kutateladze (Kutateladze &amp; Malenkov, 1978), (Blottner, 1979), (Konselov, 1966), Deckwer (Deckwer, 1980) give same o.m.(^1))&lt;br&gt;In most cases (SOCRAT excepted) h_{conv} &gt;&gt; h_{conv}<em>{ox} (\text{excepted for COSACO, SOCRAT where h}</em>{conv} \text{ ox} \sim h_{conv} \text{ ox})&lt;br&gt;Range of lateral/axial convection ratio hlat/ hax: MEDICIS: =1; TOLBIAC-ICB: 1 to 3 versus concrete type</td>
<td>Solutal convection not described&lt;br&gt;Lack of data on 2D hconv profile</td>
<td>Low impact of hconv level&lt;br&gt;High impact of ratio hlat/ hax if “crust model”&lt;br&gt;Low impact of ratio hlat/ hax if no crust model because hconv is &gt;&gt; hint</td>
</tr>
<tr>
<td>Oxide/metal heat transfer ((h_{conv o/m})</td>
<td>All codes, CORQUENCH, MAAP excepted</td>
<td>Greene’s correlation (Greene &amp; Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1968) is used in MEDICIS, TOLBIAC-ICB; Werle’s correlation in WECHSL (Werle, 1982), Blottner’s one (Blottner, 1979) in SOCRAT&lt;br&gt;h_{conv} \text{ o/m} &gt;&gt; h_{conv} \text{ ox}(excepted for COSACO, SOCRAT where h_{conv} \text{ o/m} \sim h_{conv} \text{ ox})&lt;br&gt;Limited impact of crust at oxide/metal interface&lt;br&gt;Heat conduction for CORIUM-2D</td>
<td>No correlation validated for reactor conditions (transport properties, bubble size)</td>
<td>High on axial ablation kinetics in case of stratification (depends on reactor design and concrete composition)</td>
</tr>
<tr>
<td>Oxide/metal pool configuration evolution</td>
<td>All codes, CORQUENCH, MAAP excepted</td>
<td>BALISE based criterion (Tourniaire, Seiler, &amp; Bonnet, 2003): Jg &gt; bHSΔp/\text{min} + minimum metal thickness thm (CORCON, MEDICIS, SOCRAT, TOLBIAC-ICB)&lt;br&gt;Mechanistic settling/entrainment model (CORCON)&lt;br&gt;COCO: user’s input, COSACO: stratification according to sign of Δp</td>
<td>No mechanistic model (no dependence on transport properties)&lt;br&gt;Excepted for non-validated CORCON model</td>
<td>High on axial ablation kinetics because it determines stratification and downwards focusing of decay power (depends on reactor design and concrete composition)</td>
</tr>
</tbody>
</table>

1: o.m.: order of magnitude 2: hint,exp: effective heat transfer coefficient at pool/concrete interface deduced from experiments

3.3.2. Pool interface models

As mentioned in section 3.3.1.1, it was pointed-out that the convective heat transfer coefficient due to gas bubbling exceeds largely the effective heat transfer coefficient in oxide melts deduced from experiments.

Moreover data from CLARA simulant experiments on 2D convection (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014) cannot account for the heat flux distribution along the pool/concrete interface in 2D ablation MCCI experiments versus the concrete but have the opposite trends. Indeed the MCCI experiments show clearly that the ratio of lateral to axial ablation and so the ratio of lateral to axial heat flux is larger than 1 in case of siliceous concrete that induces a higher corium viscosity and is equal to around 1 in
case of LCS concrete that induces a lower corium viscosity. Another aspect of CLARA results is the distribution of convective heat transfer coefficients obtained in case of a zero injected gas velocity at the pool bottom: in this case, the hlat/hbot ratio increases up to 2.2 at high viscosity and to still higher values at lower viscosity. This situation might correspond roughly to a possible scenario involving the build-up of an intact crust at the bottom interface preventing any gas flow through this interface and explaining partly the prevailing lateral ablation in case of a siliceous concrete. However this might be valid only if the bottom crust stays really completely tight to gases, which should not be the case at large scale in the reactor case. In case of LCS concrete, this scenario is clearly contradicted by the rather isotropic 2D ablation.

Consequently the convection anisotropy caused by bubble agitation within the liquid is not, at the present state of knowledge, the major reason of the dissymmetry of 2D ablation which is much more likely to be explained by the dissymmetry of lateral and axial pool/concrete interfaces; the description of heat transfer across the pool/concrete interface is therefore of prime importance.

Different approaches have been proposed:

with stable crust at the pool/concrete interface using either an equilibrium crust model or a non-equilibrium crust model or

without any crust at the pool/concrete interface.

3.3.2.1. Approach with active crust at the pool/concrete interface

This approach is used in 2 different models. The first model assumes the build-up of a crust at the pool/concrete interface in thermodynamic equilibrium with the pool (so-called “equilibrium crust model”); the pool/crust interface temperature is equal to the liquidus temperature at the pool composition (see Figure 3.3-4) and the composition of the crust increment is that of the solid in equilibrium with the liquid pool composition. This approach proposed by CEA (Spindler & Seiler, 2006) was implemented in the TOLBIAC-ICB code (Spindler, Tourniaire, Seiler, & Atkinson, 2005).

The justification is double: 1) the pool temperature in ACE tests follows approximately the same trend as the liquidus temperature; 2) in the MCCI 1D test MACE3b (Levy, 2002) the temperature was little modified by a large power increase, thus showing that the pool bulk thermal resistance is low and then the molten fraction is high even in the region close to the pool/concrete interface. However the comparison of results obtained respectively in 1D and 2D MCCI tests (Spindler & Seiler, 2010) shows that the bulk melt solid fraction is higher in 2D ablation tests in particular with siliceous concrete and consequently demonstrates that the pool/crust interface temperature may become much lower than the liquidus one in a more realistic 2D ablation configuration. This model assuming the thermodynamic equilibrium between the crusts and the pool, might be valid only in the asymptotic case with a reduced surface to volume ratio as in 1D tests and with a rather low corium viscosity as obtained with LCS concrete (Spindler & Seiler, 2010).

A variant of this model (also implemented in TOLBIAC-ICB) is to assume that the crusts have the composition of the melt at the time they are solidifying (non-equilibrium stable crusts), remain stable and that the interface temperature is nevertheless at liquidus temperature.
The second model assumes a deviation from thermodynamic equilibrium (so-called “non-equilibrium crust model”) and takes into account a slag layer and/or a gas film between the concrete and the crust, see Figure 3.3-5: this type of model was implemented in several codes: in the previous version of COCO, CORQUENCH (Farmer, 2010), CORCON (Bradley, Gardner, Brockmann, & Griffith, 1993), MAAP (MAAP, 2011), MEDICIS code (Cranga, Fabianelli, Jacq, Barrachin, & Duval, 2005), SOCRAT (Bolshov & Strizhov, 2006), WECHSL (Foit, Reimann, Adroguer, Cenerino, & Stiefel, 1995); the pool/crust interface temperature is evaluated by linear interpolation between solidus and liquidus temperatures as:

\[
T_{\text{int}} = (1 - \gamma) T_{\text{iq}} + \gamma T_{\text{sol}} \quad \text{Equation 3.3-3}
\]

or is evaluated from the threshold volumetric molten corium fraction (Cranga, Mun, Michel, Duval, & Barrachin, 2008):

\[
\frac{T - T_{\text{iq}}}{T_{\text{sol}} - T_{\text{iq}}} = \gamma \quad \text{Equation 3.3-4}
\]

where \( h \) is the BALI heat transfer coefficient, \( \delta \) the crust thickness on the considered boundary, \( \lambda \) its thermal conductivity, \( T_{\text{ox}} \) the temperature of the oxidic melt and \( T_{\text{dec}} \) the decomposition temperature of the concrete (assumed to be equal to the temperature at which its solid volume fraction is equal to 50%).

A still different model assuming that the solid fraction of the oxidic pool is non zero with crust formation along the pool/concrete interface is used by Areva in the COSACO code (Nie, Fischer, & Lohnert, 2007). In this code, two thermal resistances in series separate the oxidic pool bulk from the decomposing concrete: a convective resistance (based on the BALI correlation (Bonnet, 2000) and a conductive resistance through a crust layer. The overall solid fraction of the pool is determined by the in-built free-enthalpy minimiser on the basis of the oxidic pool composition and temperature. The obtained solid mass is assumed i) to accumulate along the sides of the oxidic pool as a crust and ii) to be suspended in the bulk of the oxidic pool (as floating crystals). The split between these locations is proportional to the magnitude of the corresponding interfacial and volumetric heat sinks. On the basis of the solid mass redistribution, the solid density and the surface area of the considered boundary, a crust thickness is calculated. The heat flux density to the concrete is then calculated with the following expression:

\[
\varphi = \frac{T_{\text{ox}} - T_{\text{dec}}}{\frac{1}{h} + \frac{\delta}{\lambda}} \quad \text{Equation 3.3-4}
\]

The non-equilibrium assumption is supported by the pool temperature measurements in CCI tests (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) showing that the pool temperature stays more or less below the liquidus temperature at the pool composition, requiring \( \gamma > 0 \).
3.3.2.2. Approach without active crust at the pool/concrete interface

This approach assumes no stable active crust, either no crust at all, in particular along the lateral pool/concrete interface, or an inert crust, built-up possibly on the horizontal interface, for example in case of siliceous concrete, with an interface thermal resistance because of a slag layer build-up (so-called “no crust model”, see Figure 3.3-6): this type of model is proposed with some differences by IRSN, GRS and VTT. Models from IRSN (Cranga, Mun, & Marchetto, 2010) and GRS, (Spengler, 2007), (Spengler, 2012), are implemented in the MEDICIS code; the VTT model (Sevón, 2008) was introduced in a MCCI code called FINCCI (Sevón, 2011) and developed recently.

A first justification of this approach was that in the ARTEMIS 1D test series it was found with ASTEC/MEDICIS recalculations using such an approach, that the pool temperature follows approximately the same evolution trend as the liquidus temperature. But in all tests (except for the first one because of different initial conditions) the temperature deviates significantly from the liquidus value staying above or below depending on the boundary conditions (ablation rate and superficial gas velocity); consequently crust formation at the pool/concrete interface does not take place in most cases at thermodynamic equilibrium with the pool.

A more recent justification of this approach came from the significant decrease of the pool temperature below the liquidus one in 2D MCCI tests (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010) in particular with siliceous concrete while keeping a sustained lateral ablation, suggesting that the pool/concrete interface temperature and even the pool/crust interface temperature (if any crust is present) are not directly related to the pool corium thermo-chemistry data. Moreover no evidence of crusts along the pool/concrete interfaces was found from post-test examinations.

The approach retained recently in the MEDICIS code by IRSN assumes no crust build-up along the pool/concrete interface. In this approach, the effective heat transfer coefficient from the pool bulk to the concrete interface is obtained from the equation:

\[ h_{\text{eff}}^{-1} = h_{\text{slag}}^{-1} + h_{\text{conv}}^{-1} \quad \text{Equation 3.3-5} \]

where \( h_{\text{slag}} \) is the heat transfer coefficient of the slag layer or equivalently of the pool/concrete interface and \( h_{\text{conv}} \) is the convective heat transfer coefficient from the pool bulk to the slag layer, both coefficients possibly depending on the interface orientation.

The approach proposed by GRS, (Spengler, 2007), (Spengler, 2012) is also based on the use of an effective heat transfer coefficient depending on the interface orientation; this effective heat transfer coefficient aims at describing the overall heat transfer from the pool bulk up to the pool/concrete interface but without a distinction between the convective heat transfer within the pool and the heat transfer across the slag layer. This model was implemented as an option into the MEDICIS code. Although this model is still more simplified, its application to 2D MCCI tests confirms the approach proposed by IRSN ignoring any solidifying stable crust along the interface.
3.3.2.3. **Upper pool interface models**

Although the upward heat transfer does not impact the anisotropy of 2D ablation, it influences the fraction of power radiated upwards, the value of the average ablation depth and the level of the pool temperature. Most models implemented in MCCI codes for describing the upwards heat transfer from the pool bulk use the following assumptions:

- Upwards convective heat transfer evaluated from a convective heat transfer correlation;
- Conduction across the upper crust formed on the top pool interface assuming the thermodynamic equilibrium is reached or not as in the case of the first two pool/concrete interface models.

Another approach proposed by GRS (Spengler, 2012) evaluates the upwards heat flux from the bulk pool to the upper crust using an overall heat transfer coefficient, which is constant and in most cases equal to around 300 W/m²/K. A very similar approach is also used by VTT (Sevon, 2011). It has to be pointed out that these assumptions are derived from an overall fitting against experiments and ignore the possible impact of an upper crust build-up at some imposed interface temperature.

Areva in the COSACO code (Nie, Fischer, & Lohnert, 2007) describes the build-up of an upper crust whose thickness is depending on the heat flux distribution along the pool interfaces but without imposed pool/crust interface temperature, as explained above in section 3.3.2.1 at the opposite of other approaches dealing with an upper crust.

3.3.2.4. **Synthesis on pool interface models**

Features and limitations of available models are displayed for each type of phenomena on Table 3.3-2. Specific limitations of models implemented only in some codes are outlined in red. Overall limitations are outlined in purple.

Heat conduction behind the ablation front is neglected in most codes: this approximation is justified in a quasi-state regime and in particular in medium and long term MCCI phases in the reactor case (beyond around 5 hours); this is not the case in experiments with a thinner concrete basemat especially in the late phase.

Following remarks can be made on pool interface models (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014):

1. **Pool/concrete interface model:** two main model approaches are available, a first one assuming a stable crust and a second one assuming no stable crust along the pool/concrete interface and hence imposing no precise pool/crust interface temperature. The detailed analysis on one side of available MCCI test results and on the other side of CLARA data on 2D heat convection driven by gas bubbling leads to the following conclusions: the profile of the thermal resistance along the pool/concrete interface, instead of 2D convection, is likely to be the main explanation of the 2D ablation. This is in favour of the “no crust” model approach at least in the short and medium MCCI phases which can be investigated in MCCI experiments. More precisely a possible explanation of the anisotropy observed with siliceous/clinker concrete, might be based on the increase of the thermal resistance at the bottom concrete interface due to the pile-up of solid concrete aggregates below an unstable crust or possibly a viscous slag layer. However the later evolution of the thermal resistance in particular at the bottom interface in the long term MCCI phase remains unknown. In case of LCS concrete, which releases only tiny aggregates easily entrained into the pool, the thermal resistance of pool/concrete interface stays rather uniform and is also largely prevailing over the bulk pool resistance because of the high convective heat transfer at lower corium viscosity. The pool/concrete interface model impacts also on the bulk pool temperature evolution: on one side the models with a lower pool/crust interface temperature or without any crust lead to a lower pool temperature are partially supported by experimental...
data; on the other side models with a higher pool/crust interface temperature, closer to the pool liquidus temperature are also in broad agreement with experiments if taking into account segregation of refractory material into built-up crusts, although to a less extent in case of experiments with siliceous concrete; however the latter models suppose the existence of refractory crusts along the pool/concrete interfaces which was not demonstrated up to now by metallographic PTE1.

Pool upper crust model: Due to uncertainties on experimental boundary conditions in particular at the top of the pool, it is difficult to select the best assumptions for the upwards heat transfer. However the modelling with a low pool/crust interface temperature or without any crust seems to be supported by experiments showing in most cases a thin crust with a crust renewal process or even no upper crust at all in dry situation. The use of the same value of convective heat transfer towards the pool upper interface as towards pool/concrete interfaces latter model also keeps a better consistency with CLARA 2D heat convection data, which is not possible in case of a high pool/crust interface temperature which requires an increase of upwards convective heat transfer compare to lateral one in order to get a correct prediction of the upwards radiative heat losses in 2D MCCI experiments. Such a dissymmetry could be explained if higher viscosity is considered only for pool/concrete heat transfer traducing the presence of a slag layer.

Table 3.3-2: Status on pool/concrete interface modelling

<table>
<thead>
<tr>
<th>Interfaces</th>
<th>Model type</th>
<th>Codes including its description</th>
<th>Available models/retained assumptions</th>
<th>Limitations</th>
<th>Impact on MCCI predictions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pool/concrete interface</td>
<td>“with crust” model</td>
<td>All codes at least as an option (excepted MEDICIS-GRS)</td>
<td>Crust Tsolidification$^3$, Between Tsolidus (CORCON, SOCRAT, WECHSL,..) and Tliquidus (TOLBIAC-ICB); Fm,vol,threshold ~0.5 (MEDICIS-IRSN model 1); Meshed crust and mushy zone in SOCRAT</td>
<td>No stable crust observed in exp. No mechanistic model for the influence of concrete type</td>
<td>Medium impact on pool temperature and ablation kinetics</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>No exp. data</td>
<td>Low impact expected if crust is absent</td>
</tr>
<tr>
<td></td>
<td>“no crust” model</td>
<td>MEDICIS IRSN, MEDICIS-GRS, CORCON, CORQUEENCH (as an option)</td>
<td>For MEDICIS-IRSN, MEDICIS-GRS: hint$^2$, bottom: 80 to 300 W/m$^2$/K depending on concrete type; hint$^2$, lateral: 200 to 300 W/m$^2$/K depending on concrete type; MEDICIS-IRSN (model 2); hint$^2$ combined with hconv$^v$; MEDICIS-GRS: hint includes convection CORCON, CORQUEENCH; gas film model, modified Bradley’s correlation, Sevón’s correlations</td>
<td>No mechanistic model for the influence of concrete type</td>
<td>High impact on 2D ablation profile</td>
</tr>
<tr>
<td>Upper pool interface</td>
<td>“with crust” model</td>
<td>All codes excepted MEDICIS-GRS</td>
<td>Crust Tsolidification between Tsolidus (CORCON, SOCRAT, WECHSL,..) and Tliquidus (TOLBIAC-ICB); Fm,vol,threshold ~0.5 (MEDICIS-IRSN); meshed crust in SOCRAT</td>
<td>Radiative heat transfer (Stefan’s law) Uncertainty on Tsolidification and on radiation absorption by aerosols</td>
<td>High impact on ablation kinetics because it determines the upwards heat losses</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Predicts no upper crust; unreliable extrapolation to reactor case</td>
<td></td>
</tr>
<tr>
<td></td>
<td>“no crust” model</td>
<td>MEDICIS-GRS</td>
<td>Effective heat transfer derived from exp.</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

1: hconv: convective heat transfer coefficient within bulk pool; 2: hint: heat transfer coefficient at pool/concrete interface; 3: crust formation temperature

1. This point is still an open issue as in MACE or CCI tests, some post-test measurements of uranium distribution inside the melt show higher uranium concentration near the bottom concrete interface. It is nevertheless difficult to conclude between the existence of a solid crust during the transient or the possibility to have a segregation process during the final cool-down.
3.3.3. Coolability models

Features and limitations of available coolability models are displayed for all phenomena (bulk cooling before crust build-up onset, film boiling heat transfer and radiation before crust quenching onset, water ingression and melt eruption after crust quenching onset) are listed on Table 3.3-3.

Specific limitations of models implemented only in some codes are outlined in red. Overall model limitations are outlined in purple.

Taking into account Table 3.3-3, following comments can be made on available coolability models In case of water injection:

Only radiative heat transfer from the pool upper interface to water is described by all codes;

Film boiling heat transfer is not described by some codes; moreover the “classical” correlations for the film boiling regime are not adequate for describing heat transfer during MCCI and the transition from film boiling to nucleate boiling regime is very likely much faster than predicted by and occurs at a much higher heat flux level than predicted by “classical” correlations.

Phenomena of water ingression and melt eruption are the prevailing ones. Generally speaking, two types of models are used: either simplified models or more detailed models including a deeper understanding of main physical mechanisms.

As far as water ingression is concerned, the simplified model based on Darcy’s law requires knowing the permeability of the upper crust. The more detailed model (Lomperski & Farmer, 2007) is able to evaluate this crust permeability in consistency with other physical variables determining the crust cracking due to thermal gradient such as the corium crust thermal diffusivity and linear expansion coefficient. However the values of some physical parameters such as the crust temperature at onset of cracking and other mechanical parameters taking into account the impact of crust material are required.

As far as melt eruption is concerned, the simplified model based on Ricou-Spalding’s correlation (Ricou & Spalding, 1961) is easy to use and accounts for both the variation of the gas and corium pool density. Main uncertainty is on the value of this correlation for a reactor situation with real material properties and a large scale floating upper crust. The detailed model taking into account the detailed hole geometry (Tourniaire B., Seiler, Bonnet, & Amblard, 2006) (mainly hole diameter and surface density) permits a mechanistic description of melt eruption hydrodynamics but in spite of numerous models involving in particular energy balance considerations with corium non-freezing conditions, non-flooding and non-gas-build-up conditions (Farmer, Phenomenological Modeling of the Melt Eruption Cooling Mechanism during Molten Corium Concrete Interaction, 2006), (Robb & Corradini, 2010), no really mechanistic modelling is able to predict the hole geometry in the reactor case.
### Table 3.3-3: Status on coolability models

<table>
<thead>
<tr>
<th>Phenomenon</th>
<th>Model type</th>
<th>Codes including its description</th>
<th>Available models/retained assumptions</th>
<th>Limitations</th>
<th>Impact on MCCI predictions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Heat transfer before crust build-up onset</td>
<td>Bulk cooling</td>
<td>All codes</td>
<td>Radiative heat transfer (Stefan’s law) in all codes (excepted for MEDICIS-GRS where an effective heat transfer coefficient is used) combined with bubbling enhancement (Farmer, 2010) in CORCON and CORQUENCH codes</td>
<td>Uncertainties on radiation absorption by aerosols</td>
<td>Medium because of the short time period</td>
</tr>
<tr>
<td>Heat transfer after crust build-up onset and before quenching onset</td>
<td>Film boiling</td>
<td>CORCON, CORQUENCH, TOLBIAC-ICB, MAAP, SOCRAT</td>
<td>Transition film boiling curve film boiling regime based on Berenson correlation (Berenson, 1961) In SOCRAT film boiling is introduced by dependence of effective heat transfer coefficient vs time Film boiling heat flux in MAAP is imposed as the lower limit of the heat flux from the corium to water. Ignored in MEDICIS-IRSN, COCO, WECHSL</td>
<td>Heat flux in film boiling regime during quenching of a corium pool must be increased compared to literature correlations</td>
<td>Medium because of the short time period</td>
</tr>
<tr>
<td></td>
<td>Radiative heat transfer</td>
<td>All codes</td>
<td>Radiative heat transfer (Stefan’s law) in all codes (excepted for MEDICIS-GRS where an effective heat transfer coefficient is used)</td>
<td></td>
<td>Medium because of the short time period</td>
</tr>
<tr>
<td>Heat transfer after crust quenching onset</td>
<td>Water ingestion</td>
<td>All codes, CORCON, COSACO, WECHSL excepted</td>
<td>Only simplified model : Darcy’s law in MEDICIS, COCO, SOCRAT; Kutateladze model (Kutateladze &amp; Malenkov, 1978) in MAAP Detailed model: CORQUENCH, TOLBIAC-ICB, MAAP</td>
<td>Uncertainty on crust permeability value in simplified model; Uncertainties on thermo-mechanical parameters of detailed models</td>
<td>Low excepted in the early phase as long as concrete oxide fraction is low</td>
</tr>
<tr>
<td>Melt eruption</td>
<td>All codes, CORCON, COSACO, WECHSL excepted</td>
<td>Only simplified model (Ricou-Spalding (Ricou &amp; Spalding, 1961): ASTEC-MEDICIS, COCO, MAAP, SOCRAT Detailed model PERCOLA type (Tourniaire B., Seiler, Bonnet, &amp; Ambiard, 2006) with hole geometry: CORQUENCH, TOLBIAC-ICB</td>
<td>Proportionality factor in simplified model is very uncertain and questionable; large uncertainties in detailed models: no real mechanistic model for hole geometry</td>
<td>Very high</td>
<td></td>
</tr>
</tbody>
</table>
3.3.4. Material thermophysical and thermodynamic properties

3.3.4.1. Thermophysical properties

Excepted for the viscosity, thermophysical properties of mixtures are generally evaluated by codes from the linear combination of properties of individual species weighted by their mass, molar or volume fractions.

The evaluation of viscosity is different due to the high impacts of respectively the silica fraction on the liquid oxide phase viscosity and the solid mass or volume fraction on a partially molten corium/concrete mixture.

From a general point of view the accuracy of the thermo-physical property models can be considered to be sufficient, except for the viscosity of a solid liquid mixture because of large discrepancies between models of the impact of the solid fraction. In particular results obtained using Stedman’s model or Ramacciotti’s one can lead to a discrepancy between two models of one order of magnitude on the viscosity taking into account the solid fraction.

3.3.4.2. Thermo-chemical properties

Obtaining accurate thermo-chemical data for a oxide/metal corium mixture (in particular enthalpy and molten fraction) is of prime importance for evaluating the pool temperatures and material transport properties, which strongly influence heat transfer distribution within the corium pool.

Thermo-chemical data can be generated using a thermochemistry computing tool or simplified correlations fitted on available data.

The used methods for computing thermochemistry data are listed in following Table 3.3-4; three methods exist in practice for generating thermochemistry data for the corium/concrete mixture during MCCI:

- interface with a thermochemistry tool: determining molten fraction versus temperature for a corium composition ranging from the pure initial corium up to a pure concrete using the thermo-chemistry tool, generate a thermochemistry database for the whole required temperature and corium composition ranges and interpolate from this database during the MCCI calculation; this method was chosen by ASTEC/MEDICIS using the GEMINI2 solver and a detailed thermo-chemical database such as NUCLEA as in the case of ASTEC/MEDICIS code; this method is a good compromise both keeping a sufficient accuracy in generated data and minimising the computing time;
- internal coupling with a thermochemistry tool: required thermochemistry data (mainly molten fraction, enthalpy) are computed every time interval during the MCCI calculation as in the case of COSACO, CORCON, and TOLBIAC-ICB codes; this method is obviously the most precise but at the cost of an extra computing time due to the use of a Gibbs minimiser, which can often generate convergence problems; for TOLBIAC-ICB codes, when fast calculations are needed, this coupling can be shunted by using pseudo-binary diagrams;
- interpolation between data obtained from an internal thermochemistry database or from empirical correlations (in particular for solidus and liquidus temperatures) as in the case of COCO, MAAP, WECHSL, SOCRAT codes; this method is the most simple but also the less precise and its use becomes difficult in case of corium with a specific composition such as in

2. It must be stressed that mass, molar and volume fractions are significantly different in MCCI melts due to the large density differences of (U,Zr)O₂ - around 8000 kg/m³ - and concrete -around 2000 kg/m³-.
some high temperature simulant experiments or with a concrete involving significant fractions of some particular minor species, which are not taken into account in the available database.

Methods available for evaluation thermochemistry data in MCCI codes are displayed on the next table:

**Table 3.3-4:** Methods available for evaluating in MCCI codes thermochemistry data during MCCI

<table>
<thead>
<tr>
<th>Code</th>
<th>Interface with thermochemistry tool (outside MCCI code)</th>
<th>Coupling with thermochemistry tool (Gibbs minimiser+ database)</th>
<th>Obtained from thermochemistry database or correlations</th>
</tr>
</thead>
<tbody>
<tr>
<td>ASTEC/ MEDICIS</td>
<td>Yes: GEMINI2+NUCLEA database</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>COCO</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>CORCON/ MELCOR</td>
<td>No</td>
<td>Yes</td>
<td>Yes¹</td>
</tr>
<tr>
<td>CORIUM-2D</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>CORQUENCH</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>COSACO</td>
<td>No</td>
<td>Yes</td>
<td>No</td>
</tr>
<tr>
<td>MAAP</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>SOCRAT</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>TOLBIAC -ICB</td>
<td>Yes: GEMINI2+NUCLEA database (optional)</td>
<td>Yes: GEMINI2+NUCLEA database (optional)</td>
<td>Yes (default option)</td>
</tr>
<tr>
<td>WECHSL</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
</tbody>
</table>

¹: CORCON combines uses of a thermochemistry tool and of correlations?

### 3.3.4.3. Concrete ablation

The ablation process is very likely mainly related to the melting process of concrete components, which might be the prevailing mechanism leading to the concrete ablation. More details on the concrete ablation process are given in section 1.3.2 (Concrete characteristics).

The postulated threshold temperature permitting the concrete mixing with corium is called the ablation temperature.

Values of ablation enthalpy (variation of enthalpy from ambient temperature up to ablation temperature) and ablation temperature corresponding to the basemat concrete are essential to evaluate the ablation velocity and so are required to perform a MCCI calculation. These physical are either user’s input values or evaluated by the code itself.

In the first option (user’s input), they are given as user’s input. They must be derived from empirical data if available or evaluated using an external thermo-chemistry tool, which permits to calculate the concrete molten fraction and the concrete enthalpy versus temperature. More precisely the ablation temperature can be deduced from a volumetric solid fraction threshold for the mixture of all concrete oxides or for a given component (e.g. mortar).

In the second option (tabulation in the code), empirical values of ablation temperature and ablation enthalpy are tabulated for a given set of concretes.

In the third option (calculation by the code), values of ablation temperature and ablation enthalpy are determined from a thermochemistry tool which is internal to the code or coupled with the code during the MCCI calculation.
Main information on the evaluation of concrete ablation temperature and ablation enthalpy in each code is summed-up in Table 3.3-5.

Concrete ablation is assumed to occur at a single temperature although concrete is a multicomponent mixture melting over a large temperature interval. All MCCI codes use this type of assumption. Nevertheless this ablation temperature is expected to be a function of the composition of the different concrete components corresponding to the different size classes of sand, gravel and cement, see table Table 1.3-3: Order of magnitudes of concrete constituent proportions in Section 1.3.2, in particular of their different gas contents, and of the melting behaviour of these components. There is some experimental evidence that silica gravel and mortar (mixture of sand, cement and water) have different behaviours during ablation.

Table 3.3-5: Methods available for evaluating in MCCI codes concrete ablation temperature and enthalpy

<table>
<thead>
<tr>
<th>Code</th>
<th>User’s input</th>
<th>Tabulation in the code</th>
<th>Obtained from thermochemistry tool in the code</th>
</tr>
</thead>
<tbody>
<tr>
<td>ASTEC/ MEDICIS</td>
<td>Yes; evaluated from thermo-chemistry outside the code</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>COCO</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>CORCON / MELCOR</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>CORIUM-2D</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>CORQUENCH</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>COSACO</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>MAAP</td>
<td>Yes</td>
<td>Yes – for impact of water evaporation and water/CO₂ chemical release in ablation enthalpy</td>
<td>No</td>
</tr>
<tr>
<td>SOCRAT</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>TOLBIAC-ICB</td>
<td>Yes?</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>WECHSL</td>
<td>Yes?</td>
<td>No</td>
<td>No</td>
</tr>
</tbody>
</table>

Considering the way MCCI is modelled in severe accident codes, ablation temperature has a large impact on MCCI calculations for following reasons:

- It influences the interface temperature of the different pool/concrete interfaces and then the heat flux distribution since the pool interface temperature may be different for upper pool interface (not depending on the ablation temperature), the lateral one (equal to the ablation temperature in the absence of stable crust) and at the bottom oxide interface, possibly depending on the ablation temperature in case of a pool stratification or in the absence of stable crust.

- It influences also the bulk pool temperature because of the impact on the corium pool interface temperature at least in case of no stable crust and again this impacts the energy balance because of the energy required to heat the concrete to the bulk pool temperature.

We recall here the classical formulation of corium pool energy balance during MCCI. The energy balance equation may be written as:

$$ m \left( \frac{dh}{dt} \right) = P_{\text{decay}} - P_{\text{upwards}} - \frac{dm_{\text{abl}}}{dt} \left[ h_{\text{conc}}(T) - h_{\text{conc}}(T_0) \right] $$

Equation 3.3-6
where:

- \( m \) the mass of the corium pool, \( h \) the corium enthalpy, \( P_{\text{decay}} \) the decay power, \( P_{\text{upwards}} \) the upwards heat loss rate,

\[- \frac{dm_{\text{abl}}}{dt} [h_{\text{conc}}(T) - h_{\text{conc}}(T_0)] \]

is the heat sink due to ablation and heating of the ablated concrete mass and chemical reactions,

\[- \frac{dm_{\text{abl}}}{dt} \]

is the ablated mass rate, \( h_{\text{conc}}(T) \) the enthalpy of ablated concrete at the bulk pool temperature \( T \), \( T_{\text{abl}} \) the ablation temperature and \( T_0 \) the ambient temperature (298K),

\[
\phi = \frac{dS}{dt} \]

is the mass ablation rate, \( \phi \) is the local heat flux at the corium/concrete interface, \( \int \phi \, dS \) the integral of this heat flux along the concrete ablation boundary and

\[
\Delta h_{\text{abl}} = (h_{\text{conc}}(T_{\text{abl}}) - h_{\text{conc}}(T_0)) \]

is the concrete enthalpy variation from the ambient temperature up the ablation temperature.

When examining the previous energy balance equation, it appears that the value of the ablation temperature impacts the ablation enthalpy and also the local heat flux (by imposing the pool/concrete interface temperature) and influences clearly the energy balance. The exception is the case where the pool interface temperature is not dependent on the ablation temperature because of a stable crust built-up at a solidification temperature on each concrete interface during the whole MCCI phase and in the absence of oxide/metal stratification. Hence the need appeared since a long time to evaluate precisely the concrete ablation temperature in order to achieve realistic MCCI calculations.

Available values of the concrete ablation temperature are very empirical and were obtained often indirectly from first MCCI experiments (Pechs, Skokan, & Reimann, 1979), (Skokan, Holleck, & Pechs, 1979), (Powers & Arellano, 1982-1) (Powers & Arellano, 1982-2) and then later from some extended programs: (Alsmeyer H., et al., 1995), (Thompson, Farmer, Fink, Armstrong, & Spencer, 1997). Most empirical values of concrete ablation temperatures were found within the range: 1 500 to 1 700K.

However due to the lack of precise data for each concrete type, a more reliable evaluation procedure was needed. This was the purpose of a recent work at IRSN (Barrachin, 2011) towards defining this evaluation procedure. In the frame of this work, a wide review of experimental data deduced not only from large-scale MCCI tests but also from past thermodynamic studies and separate effect experiments was performed in order to estimate the ablation temperature for the different types of concretes defined by their mass ratio \( \text{SiO}_2/(\text{SiO}_2+\text{CaO}) \) called “\( r \)” (calcareous with a \( r \) value below 0.3, limestone common sand with an \( r \) range between around 0.35 and 0.65, siliceous concrete with a “\( r \)” value above around 0.75). Detailed compositions of cements, aggregates and mortars (mixture of cement and sand made of finer aggregates of size below 4 to 6 mm) for a large set of real concretes of French PWR reactors were taken into account. Some features for a first set of limestone-sand (LCS) concretes including typical examples of French PWR reactors (named RLCS1 to RLCS4), LSL concrete (LCS concrete used in ACE tests) and VULCANO VB-U6 concrete are documented in the table Table 3.3-6.
Table 3.3-6: Features of LCS concretes

<table>
<thead>
<tr>
<th>Concrete name</th>
<th>Type of concrete components</th>
<th>ratio of concrete</th>
<th>ratio of mortar</th>
</tr>
</thead>
<tbody>
<tr>
<td>RLCS1</td>
<td>calcareous aggregates / siliceous mortar</td>
<td>0.376</td>
<td>0.567</td>
</tr>
<tr>
<td>RLCS2</td>
<td>calcareous aggregates / siliceous mortar</td>
<td>0.443</td>
<td>0.616</td>
</tr>
<tr>
<td>RLCS3</td>
<td>silico-calcareous aggregates / silico-calcareous mortar</td>
<td>0.485</td>
<td>0.420</td>
</tr>
<tr>
<td>RLCS4</td>
<td>silico-calcareous aggregates / siliceous mortar</td>
<td>0.646</td>
<td>0.554</td>
</tr>
<tr>
<td>LSL</td>
<td>calcareous aggregates / siliceous mortar</td>
<td>0.53</td>
<td>0.715</td>
</tr>
<tr>
<td>VB-U6</td>
<td>silico-calcareous aggregates / silico-calcareous mortar</td>
<td>0.379</td>
<td>0.37</td>
</tr>
</tbody>
</table>

Some features for a second set of siliceous concretes including typical examples of French PWR reactors (named RS1 to RS4), LSL and VULCANO VB-U5 concrete are documented in Table 3.3-7. In this table, an example of a hybrid concrete with siliceous aggregates and calcareous mortar (called RH1) is also provided.

Table 3.3-7: Features of siliceous concretes

<table>
<thead>
<tr>
<th>Concrete name</th>
<th>Type of concrete components</th>
<th>ratio of concrete</th>
<th>ratio of mortar</th>
</tr>
</thead>
<tbody>
<tr>
<td>RS1</td>
<td>siliceous aggregates/siliceous mortar</td>
<td>0.791</td>
<td>0.687</td>
</tr>
<tr>
<td>RS2</td>
<td>siliceous aggregates/siliceous mortar</td>
<td>0.792</td>
<td>0.68</td>
</tr>
<tr>
<td>RS3</td>
<td>siliceous aggregates/siliceous mortar</td>
<td>0.793</td>
<td>0.691</td>
</tr>
<tr>
<td>RS4</td>
<td>siliceous aggregates/siliceous mortar</td>
<td>0.807</td>
<td>0.680</td>
</tr>
<tr>
<td>VB-U5</td>
<td>siliceous aggregates/siliceous mortar</td>
<td>0.797</td>
<td>0.69</td>
</tr>
<tr>
<td>RH1</td>
<td>siliceous aggregates/calcareous mortar</td>
<td>0.591</td>
<td>0.302</td>
</tr>
</tbody>
</table>

For the calcareous refractory concretes, this analysis showed that a specific concrete ablation temperature cannot be really determined on the basis of the available data. Indeed if a criterion based on the liquid fraction is retained, the thermodynamic calculations logically lead to high “ablation” temperatures which are not really observed in MCCI tests. For these MCCI tests, the experimental observations seem to enhance the important role played by some species of the corium (the iron oxides in particular) on the concrete degradation. When the concrete is refractory, chemical interaction with some specific species of the corium or preferential interaction between some species of the concrete may contribute to lowering the ablation temperature which is intrinsically high due to the high CaO content in the calcareous concrete.

Figure 3.3-7: Liquefaction curves of some tested (and French PWR) limestone common sand concrete mortars (NUCLEA calculations)
For the limestone common sand (LCS) concretes, the analysis (see Figure 3.3-7) showed that a specific concrete ablation temperature for this type of concrete defined from an absolute criterion does not really exist. It really depends on the chemical composition of the aggregates. An LCS concrete with a lower r ratio (e.g. RLCS1 reactor concrete) contains only siliceous sand of low granulometry but a large amount of calcareous aggregates, which are dispersed into powder at decarbonation and mixed with other concrete components. So separation between mortar and aggregates does not occur during melting and the mortar melting alone cannot be a relevant indication for the ablation process. The bulk concrete with a higher calcia content (see RLCS1) has to be considered and the ablation temperature derived from this bulk concrete melting is much higher than that of RLCS2 concrete (see Figure 3.3-7).

As for calcareous concretes, other factors such as the detailed corium composition could eventually play a role in the reduction of the ablation temperature. In case of an LCS concrete with a higher r ratio (e.g. RLCS3 and RLCS4 reactor concretes), the undissolved part of large aggregates at the time of the ablation process could be more significant since both concretes contain a non-negligible content of refractory siliceous gravels. For both of these latter concretes, defining the ablation temperature from the mortar melting could be more relevant than defining it from the bulk concrete composition.

Nevertheless for most LCS concretes, the ablation temperature can be determined as the temperature corresponding to 50% of liquid in the concrete at least for LCS concretes with a high enough silica content (e.g. RLCS2 reactor concrete).

For the siliceous concretes which are frequently encountered in particular in French PWRs, the ablation temperature can be chosen in the temperature interval between 1 650 and 1 700 K on the basis of the liquefaction of the mortar (see Figure 3.3-8), since the refractory siliceous aggregates remain solid and separate from mortar during concrete melting. Moreover taking into account the uncertainty of alumina content in the French PWR mortar could lead to a reduction up to 100 K, i.e. 1 550-1 600 K.

No rule could be proposed in case of untypical “hybrid” concretes with for example a refractory mortar rich in CaO but a high fraction of mostly siliceous aggregates (see RH1 in Table 3.3-7). For this latter concrete type, a temperature of 2000 K is recommended for lack of a better model.

Efforts for improving the method for the ablation temperature assessment should be focused on following aspects:

1. Confirmation of the obtained preliminary trends are required, especially on the impact of mortar melting on the ablation process in case of silica aggregates.
2. Application to intermediate concretes (between siliceous and more calcareous LCS concrete types), or different concretes such as CCI2 (significant MgO fraction).
3. Identification and quantification of other phenomena or factors determining the ablation, e.g. mechanical behaviour or liquefaction curve of bigger aggregates (calcareous and siliceous), investigation of the influence of a third major species such as iron oxide in addition to other major species (silica, calcia).
4. Last but not the least study of the influence of iron bars on the ablation process, which remain so far mostly unknown.
3.4. Code application limitations

3.4.1. Model simplifications

Several simplifications appear in the models of several codes:

1. Heat conduction is neglected behind the ablation front in MEDICIS, COSACO, WECHSL codes.
2. Corium masses stored in crusts along the pool/concrete interface are not taken into account in several codes (e.g. MEDICIS).
3. Main transport properties of mixtures (viscosity excepted) are evaluated using a mass or volume averaging on the individual species; note that mass and volume average yield to fairly different results due to the large spectrum of densities within core-concrete mixture constituents.
4. The cavity shape may be non-realistic if some simplified assumptions are used, such as a cylindrical cavity as in CORQUENCH codes.
5. The attenuation of radiative heat flux above the corium pool by aerosols is described by a user’s parameter or a very simple evaluation of an attenuation factor.
6. The structure of the pool/concrete interface is described in all codes by rather very simple models (stable crust or absence of crust with an imposed effective heat transfer coefficient depending on the interface orientation or a gas film model).

The simplifications 1 to 3 are reasonable and the impact is limited at least in the quasi-state regime and at the reactor scale. Nevertheless, the assumption 1 can lead to a too fast ablation in experiments with rather thin basemat such as VULCANO tests because conduction heat losses are ignored.

The impact of simplification 4 might be significant but the uncertainties on the aerosol features (concentration and size distribution) are too high to permit a gain with a more mechanistic model.

The impact of simplification 5 is questionable if the aim is to obtain precise predictions of basemat melt-through in the long-term phase of a reactor case.
The simplification 6 is questionable but the use of more complex models for the pool/concrete interface would be difficult because the experimental database for validation is too scarce and it would make the code complex because of the number of input additional physical parameters and possibly numerical instabilities (e.g. in case of crust instability model). However a crust instability model is available in CORQUENCH code (Farmer, 2010).

3.4.2. Types of scenarios which can be described

Outside model limitations given above, code limitations concern the types of scenarios which can be described. Indeed the basis scenario of a corium pouring in a dry reactor pit assuming a fast corium spreading over the whole available area can be described by any available MCCI code.

However scenarios involving a corium spreading phase and still more presence of water with a more or less sustained water injection might change drastically the initial corium geometry and the subsequent evolution of the ablated cavity:

- In case of a corium pouring on a reduced part of the total available area, the real extent of corium spreading should be taken into account and might influence the ablation kinetics because of the reduced contact area between the corium inventory and the bottom and lateral concrete pit walls.
- In presence of water initially in the reactor pit, first this initial water amount will quench at least a part and possibly the total corium inventory, thus delaying the corium melting and the onset of concrete ablation but by approximately 1 hour only. Second the injected water might also hinder the corium spreading and limit the corium/concrete interaction to a restricted area thus leading to a local higher heat flux and faster concrete ablation at least before some re-spreading occurs.

Most of MCCI codes are unable to model properly the corium spreading. At least some of them can describe the impact of water injection and more precisely of the top quenching, but keeping the initial reactor pit geometry, excepted in case of MELCOR; in particular the possible restriction of concrete ablation to a part of reactor pit walls due to a localised corium accumulation because of water present in other places of the reactor pit cannot be treated or at the opposite the additional corium spreading of corium outside the initial cavity for example in the case of a concept permitting the spreading in additional cavities localised around the first one.

3.4.3. Possible reactor pit geometries

The choice of cavity or reactor pit geometries in MCCI codes is limited. Most codes can treat either slab geometry (with two ablatable lateral walls) in case of some MCCI experiments (ANL-CCI facility) or an axisymmetric cavity (cylinder or succession of truncated cones).

Nevertheless the treatment of non-axisymmetric cavities for example with a lateral corridor or large sumps (as in Fukushima Daiichi drywells) is not possible; only adaptations keeping axisymmetry and changing the surface over volume ratio can be performed but leading in fact to a rather unrealistic shape. The main reason is that the lumped-parameter approach used in most MCCI codes and leading to averaging physical quantities such as the corium temperature over the whole pool can be a reasonable simplification only if assuming an axisymmetric pool.

3.4.4. Concrete features

A question arises: which concrete types can be addressed by MCCI codes?

3. In this case, specific spreading codes must be used
Respective impacts of siliceous and LCS concretes on MCCI behaviours are known even though the ablation pattern of concrete of intermediate compositions between the studied silica-rich and limestone-rich concretes is not clearly known. Moreover the impact of another concrete type (e.g. serpentinite) on 2D ablation behaviour is unknown so far. In this case, in order to get conservative results and the shortest melt-through delay, it is recommended at the present state of knowledge to assume an isotropic heat flux distribution the mixed oxide/metal pool and in the oxide layer during the stratified configuration phase. On the opposite, if there are concerns about lateral melt through, anisotropic ablation should be considered.

3.5. Summary of overall code qualities and limitations

Taking into account the previous analysis and the experience from users, main code qualities and limitations are tentatively synthesised in the following table. Only existing code versions are considered; improvements planned in future versions are ignored.

<table>
<thead>
<tr>
<th>Code</th>
<th>Level of model detail</th>
<th>Model choice flexibility</th>
<th>Validation state</th>
<th>Flexibility of boundary conditions, geometry, corium pour scenario</th>
</tr>
</thead>
<tbody>
<tr>
<td>ASTEC/MEDICIS</td>
<td>high for thermochemistry and heat transfer models, no concrete conduction; simplified description of debris bed and melt eruption</td>
<td>high for dry MCCI models, limited for coolability models</td>
<td>almost completed for dry MCCI models; insufficient on coolability models</td>
<td>high; limitation on initial cavity shape: no non-axisymmetric cavity</td>
</tr>
<tr>
<td>COCO</td>
<td>high for thermochemistry, heat transfer models, low for stratification model</td>
<td>various kinds of model parameters are given by input</td>
<td>extended for dry MCCI models</td>
<td>cavity: axisymmetric concrete (plus non-ablative wall)</td>
</tr>
<tr>
<td>CORCON/MELCOR</td>
<td>high for thermochemistry, heat transfer and stratification models / no coolability models</td>
<td>high for pool config. evolution, reduced for pool/concrete interface</td>
<td>extended for dry MCCI models</td>
<td>high; limitation on initial cavity shape: no non-axisymmetric cavity</td>
</tr>
<tr>
<td>CORQUENCH</td>
<td>high for pool/concrete interface, crust and coolability models, no oxide/metal models; simplified cavity shape</td>
<td>high in particular for coolability models</td>
<td>extended for dry MCCI models; in progress on coolability models</td>
<td>high; limitation on initial cavity shape: no non-axisymmetric cavity</td>
</tr>
<tr>
<td>CORIUM2D</td>
<td>mass-energy conservation</td>
<td>very fast running</td>
<td>mainly for in-vessel and ex-vessel confinement structures; extended for dry MCCI models</td>
<td>high; applicable to axisymmetric cavities</td>
</tr>
<tr>
<td>COSACO</td>
<td>high for thermochemistry and heat transfer models, low for pool config. evolution and coolability</td>
<td>in general limited; low for pool/ concrete interface and pool config. evolution</td>
<td>extended for dry MCCI models</td>
<td></td>
</tr>
<tr>
<td>MAAP</td>
<td>high for pool/concrete interface, simplified heat transfer distribution, no oxide/metal pool models; multiple cavity shapes to choose from; high for coolability models</td>
<td>parametric heat transfer model (user-provided heat transfer coefficients). Multiple cavity shape models to choose from. Parametric or mechanistic coolability models available</td>
<td>extended for dry MCCI models; coolability models compared with CCI-2 and CCI-3 tests</td>
<td>high; Users can specify pouring conditions such as compositions, duration and temperature. limitation on initial cavity shape: non-axisymmetric cavity is not allowed</td>
</tr>
<tr>
<td>Code</td>
<td>Level of model detail</td>
<td>Model choice flexibility</td>
<td>Validation state</td>
<td>Flexibility of boundary conditions, geometry, corium pour scenarii</td>
</tr>
<tr>
<td>-----------</td>
<td>---------------------------------------------------------------------------------------</td>
<td>------------------------------------------------------------------------------------------</td>
<td>------------------</td>
<td>---------------------------------------------------------------------</td>
</tr>
<tr>
<td>SOCRAT</td>
<td>high for pool/concrete interface, lack of gas-driven convection; simple coolability models</td>
<td>high – most model parameters can be defined by user</td>
<td>in progress</td>
<td>high;</td>
</tr>
<tr>
<td>TOLBIAC-ICB</td>
<td>high for thermochemistry and heat transfer models, optional treatment of concrete conduction; simplified description of debris bed and melt eruption</td>
<td>limited for pool/concrete interface (crust stability and pool/crust interface temperature) and heat flux distribution in oxidic layer; high for coolability models</td>
<td>extended for dry MCCI models; validated against MACE and CCI tests for coolability models</td>
<td>high; Users can specify pouring conditions such as compositions, duration and temperature; limitation on initial cavity shape; possible simultaneous MCCI at the same time in different rooms (with possible corium transfer between rooms)</td>
</tr>
<tr>
<td>WECHSL</td>
<td>high for heat transfer models)/ simplified correlations for thermochemistry, no coolability models</td>
<td>low flexibility in particular for pool interface models (crust stability and pool/crust interface temperature) and heat flux distribution</td>
<td>extended for dry MCCI models</td>
<td>limitations on cavity geometry and initial cavity shape, no treatment of pouring?</td>
</tr>
</tbody>
</table>
4. Status of validation and model uncertainties

The preceding chapter gives a short description of a range of detailed models (e. g. for the pool interface, the convection in the pool, the coolability in a flooded scenario etc.) which are currently used in recent applications of the major MCCI simulation codes. These detailed models, which are recommended for usage by the different code users as the result of the individual code validation work, are often based on theoretical considerations for separate effects and validated for separate effect experiments. However, MCCI is essentially a complex interaction of several phenomena and it is important to know about the performance of the codes for integral MCCI experiments.

This chapter focusses not on detailed mechanistic models like those described in Chapter 3 but on more higher level capabilities of the codes, which can be validated by comparing with available data of MCCI experiments. Sections 4.1-4.3 aim at identifying these higher-level capabilities of MCCI codes. Then Section 4.4 presents the validation status of the codes with view to these capabilities. Models require the usage of material properties like the viscosity of the corium. Section 4.5 reports the status of material properties validation. Section 4.6 discusses how the data of experiments can be scaled to the reactor situation and how this is supported by available models in the codes.

4.1. General remarks on the use of data from integral experiments for code validation

As the interactions between corium and concrete are composed of a complex combination of several physical phenomena a calculation performed by a code dedicated to MCCI usually involves a complex interaction of several detailed models. MCCI experiments are integral experiments; because of that they cannot serve as a validation for each detailed phenomenological model included in the codes. Detailed models are usually validated in separate effect tests, but for the case of MCCI it is difficult to define meaningful separate effect tests, since some phenomena can hardly be separated or isolated from each other, like e. g. the heat transfer between melt and concrete interface and the melting of the concrete. Only the combination/interaction of models used in the codes can finally be validated in integral experiments. It is at first necessary to reflect how experimental data obtained from integral MCCI experiments can be scaled for code validation.

Therefore it is necessary to understand the interaction of models in an MCCI code. The development of MCCI codes should help to understand the real physics. Several dedicated MCCI codes have evolved from long-term international R&D projects on MCCI in the past. It is interesting to understand trends predicted by the models when applied to typical MCCI conditions. The following section gives some insights for the exemplary case of the MCCI code MEDICIS (Cranga, Fabianelli, Jacq, Barrachin, & Duval, 2005).

4.2. Considerations on the energy balance in MCCI

MCCI is characterised by a complex combination of heat and mass transfer processes. Neglecting energy sources and sinks from chemical reactions, the energy balance for the corium pool under dry conditions in MEDICIS is written in the form:
\[
\frac{dH}{dt} = P_{\text{decay}} - P_{\text{rad}} - P_{\text{int,conc}} + P_{\text{massflux,conc}} - P_{\text{massflux,\,gas}} \tag{4.2-1}
\]

**Equation 4.2-1**

<table>
<thead>
<tr>
<th>Symbol (unit)</th>
<th>Explanation</th>
<th>Represented in Figure 4.2-1 as</th>
</tr>
</thead>
<tbody>
<tr>
<td>(H) (J)</td>
<td>Enthalpy of corium pool</td>
<td></td>
</tr>
<tr>
<td>(P_{\text{decay}}) (W)</td>
<td>Energy source due to internal decay power</td>
<td></td>
</tr>
<tr>
<td>(P_{\text{rad}}) (W)</td>
<td>Energy flux due to radiation from the free surface</td>
<td>Red arrow at the free surface of the pool</td>
</tr>
<tr>
<td>(P_{\text{int,conc}}) (W)</td>
<td>Energy flux due to convection to the concrete interface</td>
<td>Red arrows along the concrete interface</td>
</tr>
<tr>
<td>(P_{\text{massflux,conc}}) (W)</td>
<td>Energy flux with the concrete mass flux released into the pool at the interface</td>
<td>Grey arrows along the concrete interface</td>
</tr>
<tr>
<td>(P_{\text{massflux,gas}}) (W)</td>
<td>Energy flux with the gas mass flux released from the pool at the free surface</td>
<td>Grey arrow at the free pool surface</td>
</tr>
</tbody>
</table>

**Figure 4.2-1:** Heat and mass transfer processes at the outer boundary and internal interfaces of the MCCI pool (Spengler, 2013)

Transient calculations with the MCCI code MEDICIS for a homogeneous pool configuration (e.g. the experiment CCI-2 from the NEA-MCCI Project) and using constant heat transfer coefficients between corium and concrete (as found to be fairly consistent with several experiments (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014) have shown, that the calculated pool behaviour tends to a quasi-steady state and remains in this state for a long term (if there were no transient boundary conditions), in which the internal decay power \(P_{\text{decay}}\) is nearly, except for a small quantity \(\Delta P\), balanced by convection to the concrete interface \((P_{\text{int,conc}})\) and by release at the free surface \((P_{\text{rad}})\) (Spengler, 2013), (Spengler, Fargette, Foit, Agethen, & Cranga, 2013), see Figure 4.2-2:

\[
\Delta P = P_{\text{decay}} - P_{\text{rad}} - P_{\text{int,conc}} \tag{4.2-2}
\]

**Equation 4.2-2**
Since $\Delta P$ shows to be much smaller than the sum of $P_{rad}$ and $P_{int,conc}$ in the MEDICIS calculation during the steady state regime, Equation 4.2-2 can be simplified to

$$0 \approx P_{\text{decay}} - P_{rad} - P_{int,conc}$$

for this quasi-steady state time period.

According to this finding the pool behaviour in MEDICIS can be approximated for a long term period by a kind of quasi-steady state balance, using the balance Equation 4.2-3 (redistribution of internal power to the interfaces) in a slowly varying geometry.

During the course of MCCI tests there are transient effects (e. g. at the early stage, when the melt eventually loses its superheat or some limited zirconium or chromium oxidation takes place). Nevertheless, it can be assumed that the overall process on the long term may be described by a sequence of such quasi-steady states. Since the MCCI codes aim at predicting the long-term, the sequence of quasi-steady states should be tracked by the codes. However, for specific questions like the MCCI dynamics on the short term, e. g. for the MCCI in melt retention concepts including the use of core catchers, the capturing of transient effects may be important, too.
For illustration, the effect of a transient as calculated by an MCCI code (MEDICIS) is indicated in Figure 4.2-3, where the pool temperature calculated with MEDICIS for the MCCI experiment CCI-2 is plotted. The original calculation assumes $T_0 = 2153$ K as initial temperature of the melt whereas in a variation $T_0 + 300$ K is assumed. Both calculations join into a common quasi-steady state after approx. 45 min. This quasi-steady state is determined by a rough equality of injected power and heat fluxes to the surface according to Equation 4.2-3 and the pool temperature adapts to the effectiveness of heat transfer mechanisms (which may be a function of pool temperature) and the size and shape of the growing interface area. The calculation with elevated initial temperature leads to slightly faster ablation in the early stage, but the effect on final ablation depth is relatively small (Figure 4.2-4).

From the observed trends of the MEDICIS code the following general statements can be derived:

- Steady state is given by an approximate compensation of the power injected in the melt with the power transferred at the boundaries (interface to concrete, top surface) according to Equation 4.2-3 in a fixed geometry and with a constant power. If there were no concrete erosion the pool temperature would stay constant in such steady state. However, in MCCI there is no steady state since there is concrete erosion and the pool boundaries are moving.
- In quasi-steady state the geometry and the level of power injected (in experiment and reactor case) vary slowly, so that the process can be described by a sequence of steady states due to Equation 4.2-3. MCCI code calculations tend to such a quasi-steady state behaviour.
• However, the whole energy balance is respected only if Equation 4.2-1 is fulfilled, which results under typical conditions in a temperature decrease over the whole time period considered.

• With typical heat transfer coefficients in the order of a few 100 W/(m² K) it takes the order of 1 hour experimental time until a quasi-steady-state establishes. The preceding temperature transient is affected by melt thermal properties like specific heat, density, latent heat evolution, void fraction etc. Differences in material properties would lead to different courses of temperature evolution (i.e. slower or faster) computed by the codes during such transient. This makes the precise recalculation of the temperature transient in short-running experiments (~ order of 1 h) very ambitious.

• Since the variation of geometry and decay power is slow during typical MCCI conditions, the quasi-steady approach should be adequate for the long term.

• The validation of the complete energy balance, Equation 4.2-1, requires the calculation of correct ablation volume (indicated by geometrical ablation depths) and correct pool temperature behaviour.

• A validation of the different terms in the energy balance (including the relative magnitudes of different heat fluxes) is in most cases not accurate, since neither the ablation volume (as a measure for the integral of $P_{\text{int,conc}}$) nor the heat transferred from the top surface (integral of $P_{\text{rad}}$) are known in experiments with satisfying precision. However, assuming that the experiment is running in a quasi-steady state regime (as the models in MEDICIS suggest for slowly varying boundary conditions), the comparison of data for ablation depths and pool temperature may help to give more differentiated statements about the model validation status:

1. If heat fluxes at interfaces (i.e., top surface or interface with concrete) as computed by the codes are compared with experimental data (i.e. upward heat flux or erosion velocity) for a quasi-steady state period (and not during transient periods) a validation may be obtained for the basic capability of calculating the correct heat flux distribution in the codes resulting in a correct prediction of concrete ablation ($abl$)

2. If – in addition to the previous item – the melt temperature evolution ($mt$) computed by the codes is compared to experimental data during a quasi-steady state period a validation of the codes may be obtained for the effective heat flux modelling $q = h_{\text{eff}}(T - T_{\text{int}})$ which is used with potentially different model parameters $h_{\text{eff}}$ and $T_{\text{int}}$ for the various interfaces. If the MCCI runs predominantly in a quasi-steady state regime, differences in code predictions for pool temperature can be referred to different parameters $h_{\text{eff}}$ and/or $T_{\text{int}}$ for some or all interfaces of the melt pool, presumed that the heat flux distribution is correct. No further information on other models e.g. viscosity, solid fraction evolution, impact of superficial gas velocity, interface morphology etc. can be deduced.

• The quasi-steady state obtained after a transient is finally governed by the level of internal power release (which should be a common data for all codes), the computed power distribution to the different interfaces (i.e. power split in sideeward, downward and upward direction), the area of the interfaces and the effective heat transfer coefficients approximated for the specific interface.

### 4.3. Identification of phenomenological models to be validated and useful experiments

Considering the findings in Section 4.2 a list of potential higher-level, phenomenological capabilities (or models) that may be validated based on available experimental data are given in Table 4.3-1. Experiments which are proposed for model validation based on the current understanding are listed in Table 4.3-2. Not all phenomena are covered by each experiment. Available data for validating basic code capabilities are mapped here to the individual experiments.
Table 4.3-1: List of phenomenological models in MCCI codes (i.e. basic capabilities) that can be validated by integral MCCI experiments

<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Phenomenological model</th>
<th>Prerequisites and conditions for successful validation of specific model</th>
</tr>
</thead>
<tbody>
<tr>
<td>abl</td>
<td>Concrete ablation (ablation depth and 2D profile)</td>
<td>Progress of (selected) local ablation depths vs. time and the final 2D ablation contour is well met by the simulation. However, pool temperatures predicted by the code may still deviate from the measured ones, this is subject of the following item (mt).</td>
</tr>
<tr>
<td>mt</td>
<td>Melt temperature</td>
<td>In addition to abl the pool temperatures are also well met in the simulation. This requires that the assumptions for heat transfer (i.e. the product of calculated effective heat transfer coefficients $h$ with the computed driving temperature differences $\Delta T$) and the evolution of geometry represent a good approach of the experimental data. The calculated heat transfer coefficients $h$ are very specific to the assumed interface temperature. The assumption of higher interface temperatures would require larger $h$ to transfer the same heat flux to the boundary at smaller $\Delta T$.</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Phenomenological model</th>
<th>Prerequisites and conditions for successful validation of specific model</th>
</tr>
</thead>
<tbody>
<tr>
<td>gr</td>
<td>Gas release from MCCI</td>
<td>Gas release rates (CO, CO$_2$, H$_2$O, H$_2$) and integral gas masses released during MCCI are well simulated. It has to be checked that there is no bypass of gas release in the experiments (i.e., that all the gas goes through the pool and participates in chemical reactions) and that there is no release of water from the concrete before the MCCI in the test preparation phase.</td>
</tr>
<tr>
<td>ox</td>
<td>Oxidation reactions</td>
<td>Oxidation reactions of metals are simulated in agreement with experimental observations. This can be checked by comparing the species composition in the gas mass fluxes released from the pool surface and/or by comparing the species composition in the melt between calculation and experiments.</td>
</tr>
</tbody>
</table>
| tf           | Top flooding           | In the situation of a corium pool flooded from the top there is a crust growing on top of the surface of the pool and the phenomenology is different from the dry situation (boiling condition at the exterior of the crust or water ingression into the crust). In past research programs three special phenomena effecting the cooling performance were investigated:  
1. Bulk cooling: Initially there is an intense interaction between liquid melt without significant crust on top of it and water which leads to peak heat fluxes.  
2. Water ingression: In the flooded situation the heat flux between the growing top crust of the corium and the water is assumed to be controlled by the dry-out heat flux limit according to the theory of water ingression. This heat flux is elevated compared to thermal conduction through the crust. The dry-out heat flux limit generally depends on material properties of the crust.  
3. Melt eruption: Since the growing top crust of corium is porous, liquid melt is driven through the crust into the water pool above the corium, where it is fragmented and efficiently cooled. The entrained mass flow depends on the geometry of holes in the crust and on the superficial gas velocity. Resulting heat fluxes are elevated compared to thermal conduction through the crust. 

Available models for the overall effect of top flooding or for the detailed mechanisms are validated on the basis of measured heat flux data at the melt/water or crust/water interface. Since the time behaviour of phenomena 1, 2, 3 is different (1: initial peak transient, 2: elevated heat flux plateau, 3: periodic peak transients) validation may be obtained for the specific detailed models 1, 2, 3. For the melt eruption, model can be validated on the determination of the mass of particles debris. There are limitations in interpreting test results with top flooding, since in most experiments with top flooding the top crust attached to the sidewalls and thus formed an unrealistic boundary condition. |
<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Phenomenological model</th>
<th>Prerequisites and conditions for successful validation of specific model</th>
</tr>
</thead>
<tbody>
<tr>
<td>tc</td>
<td>Top crust formation</td>
<td>Due to cooling of the melt at the top (heat transfer by radiation or due to boiling of water) a crust forms on top of the corium pool. A validation of the top crust thickness requires that the growth of crust thickness is well approximated in the codes, if compared with experimental data on final crust thicknesses. However, the available experimental data on top crust thicknesses are only weak indications for real crust thicknesses during the heating period.</td>
</tr>
<tr>
<td>st</td>
<td>Stratification evolution</td>
<td>Situation of MCCI with stratified metal layer/oxide layer established due to density differences might have a strong impact on the ablation rates. Due to the enrichment with lighter concrete decomposition products the oxide layer will be settled in the long term on top of the metal layer. Mixing and stratification is highly affected by the gas flow through the melt; for high gas superficial velocities, oxidic and metallic phases will be fully mixed. The different potential configurations (mixed or stratified) will have large impact on the resulting ablation behaviour. The only validation can be obtained by comparison with the final experimental configurations characterised by the post-mortem analyses.</td>
</tr>
<tr>
<td>re</td>
<td>Effect of reinforcement</td>
<td>Rebars have the potential to strongly influence the 2D course of the MCCI once the metal at the bottom starts to freeze. A validation requires that the erosion of concrete including iron rebars is well simulated in the code if compared with available experimental data.</td>
</tr>
<tr>
<td>fp</td>
<td>FP/aerosol release</td>
<td>FP and aerosol release rates and integral masses released during MCCI are well simulated.</td>
</tr>
<tr>
<td>inc</td>
<td>Incubation</td>
<td>Due to the thermal shock between hot melt and cold concrete at the time of corium slumping from the RPV an instantaneous crust may be formed and may attach to the concrete wall. In experiments this crust is not effectively heated in contrast to the liquid melt. This initial crust insulates thermally the melt pool. Heat is transferred from the melt through the crust into the concrete but the erosion (liquefaction of concrete) is slow or non-existing. In quasi-steady state the initial crusts will be no longer present (depending on local heat fluxes). In this case the erosion velocity is elevated compared to early times. A successful validation for the incubation phenomena should predict the transient formation and failure of contact resistances as indicated by tc-measurements in experiments.</td>
</tr>
</tbody>
</table>

**Table 4.3-2: Integral MCCI experiments proposed for validation of specific models**

<table>
<thead>
<tr>
<th>Test series</th>
<th>No.</th>
<th>Provides data for phenomenological models</th>
<th>Initial melt composition</th>
<th>Concrete type</th>
<th>Geometry</th>
</tr>
</thead>
<tbody>
<tr>
<td>SURC (Copus &amp; Powers, 1992)</td>
<td>1</td>
<td>abl, mt, gr, ox, fp, inc</td>
<td>UO2, ZrO2 + Zr</td>
<td>LCS</td>
<td>1D cyl.</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>abl, mt, gr, ox, fp, inc</td>
<td>UO2, ZrO2 + Zr</td>
<td>Basaltic</td>
<td>1D cyl.</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>abl, mt, gr, ox, fp, inc</td>
<td>Steel + Zr</td>
<td>Basaltic</td>
<td>1D cyl.</td>
</tr>
<tr>
<td>SWISS (Blose, Gronager, Suo-Antilla, &amp; Brockman, 1987)</td>
<td>1</td>
<td>abl</td>
<td>Steel</td>
<td>LCS</td>
<td>1D cyl.</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>abl, gr, ox, tf</td>
<td>Steel</td>
<td>LCS</td>
<td>1D cyl.</td>
</tr>
<tr>
<td>WETCOR (NRC, 1993)</td>
<td>1</td>
<td>abl, mt, tc, tf</td>
<td>Alumina based oxide</td>
<td>LCS</td>
<td>1D cyl.</td>
</tr>
<tr>
<td>BETA (Alsmeyer, et al., 1992)</td>
<td>V1.8</td>
<td>abl, mt, gr</td>
<td>Steel + alumina based oxide</td>
<td>Siliceous</td>
<td>2D cyl.</td>
</tr>
<tr>
<td></td>
<td>V2.3</td>
<td>abl, mt, gr</td>
<td>Steel + alumina based oxide</td>
<td>Siliceous</td>
<td>2D cyl.</td>
</tr>
<tr>
<td>Test series</td>
<td>No.</td>
<td>Provides data for phenomenological models</td>
<td>Initial melt composition</td>
<td>Concrete type</td>
<td>Geometry</td>
</tr>
<tr>
<td>-------------</td>
<td>-----</td>
<td>------------------------------------------</td>
<td>--------------------------</td>
<td>---------------</td>
<td>----------</td>
</tr>
<tr>
<td>V3.2</td>
<td>abl, mt, gr, cs</td>
<td>Steel + alumina based oxide</td>
<td>LCS</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>V3.3</td>
<td>abl, mt, gr</td>
<td>Steel + alumina based oxide</td>
<td>LCS</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>V5.1</td>
<td>abl, mt, gr, ox</td>
<td>Steel + alumina based oxide + Zr</td>
<td>Siliceous</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>V5.2</td>
<td>abl, mt, gr, ox, fp</td>
<td>Steel + alumina based oxide + Zr</td>
<td>Siliceous</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>V5.3</td>
<td>abl, mt, gr, ox, fp</td>
<td>Steel + alumina based oxide + Zr</td>
<td>Siliceous</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>ACE (Sehgal &amp; Spencer, ACE Program Phase C: Fission Product Release from Molten Corium Concrete Interaction MCCI,, 1992)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L2</td>
<td>abl, mt, gr, ox, fp</td>
<td>Oxidic corium + Zr</td>
<td>Siliceous</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>L5</td>
<td>abl, mt, gr, ox, fp</td>
<td>Oxidic corium</td>
<td>LCS</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>L6</td>
<td>abl, mt, gr, ox, fp</td>
<td>Oxidic corium + Zr</td>
<td>Siliceous</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>L8</td>
<td>abl, mt, gr, ox, fp</td>
<td>Oxidic corium + Zr</td>
<td>Limestone</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>MACE (Farmer, Kilsdonk, &amp; Aeschlimann, 2009) (Farmer, Spencer, Binder, &amp; Hill, 2001)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>M3b</td>
<td>abl, mt, gr, ox, tf, tc</td>
<td>Oxidic corium + Cr</td>
<td>LCS</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>M4</td>
<td>abl, mt, gr, ox, tf, tc</td>
<td>Oxidic corium + Cr</td>
<td>Siliceous</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>M1b</td>
<td>abl, mt, gr, ox, tf, tc</td>
<td>Oxidic corium + Zr</td>
<td>LCS</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>MSET1</td>
<td>abl, mt, tf, tc</td>
<td>Oxidic corium + Cr</td>
<td>Inert</td>
<td>1D rect.</td>
<td></td>
</tr>
<tr>
<td>COTELS (Maruyama, et al., 2006)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>D-6</td>
<td>abl</td>
<td>Steel</td>
<td>Basaltic</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>COMET (Sdouz, et al., 2006) (Alsmeyer, et al., 2007)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L2</td>
<td>abl</td>
<td>Steel + alumina based oxide</td>
<td>Siliceous</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>L3</td>
<td>abl, tf</td>
<td>Steel + alumina based oxide</td>
<td>Siliceous</td>
<td>2D cyl.</td>
<td></td>
</tr>
<tr>
<td>VULCANO (Journeau C., et al., 2012) (Journeau C., et al., 2010)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>VB-U5</td>
<td>Abl, (mt)</td>
<td>Oxidic corium</td>
<td>Siliceous</td>
<td>2D,1/2 cyl.</td>
<td></td>
</tr>
<tr>
<td>VB-U6</td>
<td>Abl, (mt)</td>
<td>Oxidic corium</td>
<td>Siliceous</td>
<td>2D,1/2 cyl.</td>
<td></td>
</tr>
<tr>
<td>VB-U7</td>
<td>Abl, (mt)</td>
<td>Oxidic corium</td>
<td>Ferro-siliceous</td>
<td>2D,1/2 cyl.</td>
<td></td>
</tr>
<tr>
<td>VBS-U1</td>
<td>abl,(st), (ox)</td>
<td>Oxidic corium + steel</td>
<td>limestone-rich</td>
<td>2D,1/2 cyl.</td>
<td></td>
</tr>
<tr>
<td>VBS-U3</td>
<td>abl, (st), (ox)</td>
<td>Oxidic corium + steel</td>
<td>Silica-rich</td>
<td>2D,1/2 cyl.</td>
<td></td>
</tr>
<tr>
<td>VBS-U4</td>
<td>abl, (st), (ox)</td>
<td>Oxidic corium + steel</td>
<td>Silica-rich</td>
<td>2D,1/2 cyl.</td>
<td></td>
</tr>
</tbody>
</table>
4.4. Code validation status

Having clarified in the previous section, which information on higher-level phenomenological models can be derived from available experimental data, this section presents the status of code validation with regard to available information.

4.4.1. Status of individual codes

The individual status of code validation is described from a general point of view in this section for each code (in alphabetical order) separately. Additional details are provided with regard to the higher-level phenomenological model list of Table 4.3-1 in Appendix 7.2.

4.4.1.1. COCO

The COCO code is validated through the comparison with tests in the COTELS project, which was conducted by NUPEC (Maruyama, et al., 2006), and tests in the NEA MCCI Project. The tests D-6 and D-11 in the COTELS project and tests CCI-2 to -6 in the NEA MCCI Project were analysed by the COCO code.

4.4.1.2. CORCON

The CORCON module of the MELCOR code, which models core-concrete interaction phenomena, has been validated in the past against a considerably large set of experiments. A detailed description of the MELCOR code can be found in reference (Gauntt, 2005) and a detailed description of the CORCON module can be found in reference (Bradley, Gardner, Brockmann, & Griffith, 1993).
This CORCON validation set includes the SURC (Copus & Powers, 1992), SWISS (Blose, Gronager, Suo-Anttila, & Brockman, 1987), and WETCOR (NRC, 1993) experiments performed at the Sandia National Laboratories (SNL), BETA tests performed at the Karlsruhe Institute of Technology (KIT) (Alsmeyer, et al., 1992), and ACE and MACE tests performed at the Argonne National Laboratory (ANL) (Sehgal & Spencer, ACE Program Phase C: Fission Product Release from Molten Corium Concrete Interaction MCCI, 1992). These tests cover a broad range of input power conditions, different types of concrete (basaltic, limestone, limestone-common sand, and siliceous), and both metallic and oxidic melts. The chemical reactions of zirconium (and other metals) at high temperatures were also investigated in some of these tests. Moreover, several MCCI experiments were conducted with an overlying water (Spencer, et al., 1992), (Sdouz, et al., 2006) (Alsmeyer, et al., 2007), (Journeau C., et al., 2012), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, OECD MCCI Project: 2-D Core Concrete Interaction (CCI) Tests, Final Report, 2006), (Farmer, Lomperski, Kilsdonk, & Aeschlimann, 2010). These tests provide important information on the thermal-hydraulic behaviour of concrete basemat and fission product releases in the presence of overlying water.

Assessment of CORCON against the MCCI experimental data fall into three main categories:

- Heat transfer between melt and concrete, and between melt and overlying water, if present, and basemat ablation from core concrete interactions.
- Chemical reactions and gas release (mainly flammable gas production) due to core concrete interactions.
- Fission product release.

The CORCON module can be used in stand-alone mode with appropriate input and boundary conditions specified. Alternatively, it can be used within the cavity (CAV) package of the integral code MELCOR.

Recent applications of MELCOR to address ex-vessel debris coolability by an overlying water pool treated heat flux partitioning between melt, concrete, and overlying water in a parametric manner. Though such treatment yielded reasonably satisfactory results in the prediction of transient core debris cooling rates in experiments, uncertainties in parametric modelling were recognised. That is, a stable water-impervious interfacial crust is assumed to form between the core debris and overlying coolant, so even with this modelling upgrade the long term core-concrete interaction will continue unabated without the possibility of quenching and stabilising the core debris.

4.4.1.3. CORQUENCH

CORQUENCH validation has principally focused on reactor material experiments since the code does not currently possess the capability to import or evaluate material properties of simulants. Referring to Table 4.3-2, in terms of dry cavity experiments, the validation matrix includes tests L2, L5, L6, and L8 of the ACE/MCCI test series, the SURC-1 and -2 tests conducted as part of the SURC test series, and finally the CCI-2, -3, and -4 tests conducted as part of the NEA MCCI-2 Program. In terms of wet cavity tests, the matrix includes the M1b, M3b, and M4 tests conducted as part of the MACE Program; the CCI-2, -3 (late-flooded) tests conducted as part of the MCCI-1 Program, and finally the single large scale CCI-6 test featuring early cavity flooding that was carried out as part of the NEA MCCI-2 Program. A complete description of the validation activities is provided in (Farmer, 2010).

4.4.1.4. COSACO

The COSACO code has been validated against a large number of 1D and 2D MCCI tests including: MACE-M3b, MACE-M4, ACE-L5, NEA-CCI-1, BETA 5.2, CORESA 2.1, SURC4, VULCANO VB-U5 and VB-U6. The calculated concrete ablation behaviour and melt temperature show an
adequate consistency with the experimental results. Furthermore, the results of various benchmark calculations show good consistency with other MCCI codes.

On this basis, it is concluded that the general treatment of thermochemistry in COSACO and the thermal-hydraulic models specifically implemented for the oxidic and metallic melts are well-suited to analyse the characteristics of the MCCI process.

4.4.1.5. MAAP

The MCCI model in MAAP has been validated against a number of dry cavity MCCI tests:

- ACE tests L2, L5, L6 and L7 performed by Argonne National Laboratory (Thompson & Fink, 1988), (Thompson & Fink, 1989), (Thompson & Fink, 1991a), (Thompson & Fink, 1991b)
- SURC-4 test by Sandia National Laboratory (Copus, Blose, Brockmann, Gopmex, & Lucero, Core-Concrete Interactions using Molten Steel with Zirconium on a basaltic Basemat: The SURC-4 Experiment, 1989)
- BETA test series V5.1, V5.2 and V6.1 by Kernforschungszentrum Karlsruhe (see Alsmeyer, et al., 1992) and (Alsmeyer & Firnhaber, 1990)

4.4.1.6. MEDICIS

The validation work of IRSN and GRS has recently been focused on experimental data from 2D MCCI oxidic tests (NEA CCI-2,3,4,6 (Cranga, Mun, Michel, Duval, & Barrachin, 2008), (Cranga, Mun, & Marchetto, 2010); VULCANO VB-U5-U6 (Journeau C., et al., 2012), LACOMERA-COMET L2, L3 (Spindler, et al., 2007) and also by GRS only (Spengler, 2012). The analyses of these experiments led to the proposal of a parametric approach for the 2D heat transfer coefficient distribution along the melt/concrete interface in contrast to available mechanistic models.

4.4.1.7. SOCRAT/HEFEST

The SOCRAT/HEFEST verification programme has started recently, so only the rational for the selection of experiments and some particular features of the code related to the selected experiments are discussed here. Since the scope of applicability of the code should include concretes used in Russian NPPs, the list of experiments used for verification differs from Table 4.3-2, but a number of experiments from Table 4.3-2 are included.

4.4.1.8. TOLBIAC-ICB

The TOLBIAC-ICB validation matrix contains experiments from the SURC-, ACE-, MACE-, NEA-MCCI, BETA- and COMET-test series as well as on VULCANO tests. Despite the difficulties of defining appropriate initial and boundary conditions (e.g. initial melt composition, which is especially important for the phase segregation model) for MCCI tests, and despite the low accuracy of some measurements, the results of the simulations of most experiments with TOLBIAC-ICB are satisfactory, even if the melt temperature is in some cases apparently overestimated. Nevertheless the melt temperature measured at the end of the experiments corresponds to the liquidus temperature that is calculated on the basis of the measured melt composition. The major problem seems the calculations of the correct melt composition rather than the model assumption according to which the melt temperature follows the liquidus temperature. Post-test analyses show evidence that part of the initial melt is splattered on the walls or concentrates in initial crusts and that in ANL tests magnesia could be ablated or dissolved and mixed with the corium.
4.4.2. Code benchmarks on MCCI experiments

In the frame of the European SARNET network and the NEA-MCCI Project code benchmark actions have been performed to evaluate the uncertainties of the codes for the prediction of MCCI experiments (Cranga, et al., 2010), (Journeau C., et al., 2012), (Spindler, et al., 2007). A synthesis of these actions is given here. It concerns first the 2D NEA-CCI-2 test (performed at ANL (Farmer, Lomperski, Kilsdonk, & Aeschlimann, OECD MCCI Project: 2-D Core Concrete Interaction (CCI) Tests, Final Report, 2006) with a homogeneous pool and a limestone/common sand concrete, which was used for a blind benchmark. Secondly, the COMET-L2 and COMET-L3 2D experiments performed at KIT in a stratified configuration were used as a post-test (L2 (Sdouz, et al., 2006) and a blind-test (L3 (Alsmeyer, et al., 2007) benchmark. A third MCCI benchmark was performed for the VULCANO experiments VB-U5 and -U6 (Journeau C., et al., 2012).

The major uncertainties that are pointed out in these analyses concern mainly the corium/concrete interface model and the heat flux distribution along lateral and bottom interfaces in case of a fixed homogeneous pool configuration. In case of stratification, major uncertainties are related with the initial pool configuration assumptions and subsequent configuration evolution models as well as with the interlayer heat transfer.

4.4.2.1. The CCI-2 benchmark

The benchmarking work concerning the CCI-2 test (Farmer, Lomperski, Kilsdonk, & Aeschlimann, OECD MCCI Project: 2-D Core Concrete Interaction (CCI) Tests, Final Report, 2006) was performed within the NEA-MCCI Project. The results were presented at the MCCI Project seminar (Spindler, et al., 2007). The most interesting results concerning code comparisons are the blind calculations performed before the experiment. They were performed with the same data for all participants. The input data for post-test calculations of the experiment were significantly modified after the experiment (in particular concerning the corium mass involved in the interaction and the concrete composition). It was shown, that models taking a strong coupling of heat transfer models with thermodynamical equilibrium conditions into account (i.e. coupling of interface temperature between corium and concrete with the liquidus of the residual melt) depend strongly on the melt composition. However, a comparison of the post-test calculation results was not organised.

![Figure 4.4-1: CCI-2 blind benchmark: Melt temperature versus time](image)

Very large temperature differences (Figure 4.4-1) are observed for the different codes, with three kinds of initial behaviour: sharp increase, gradual decrease and sharp decrease. This behaviour is related to the condition that is used for the heat transfer between melt and concrete. An interfacial temperature equal to the liquidus temperature gives an increase of the melt temperature because the
initial temperature is lower than the calculated liquidus temperature. On the opposite, an interfacial
temperature equal to the solidus temperature gives an initial decrease of the melt temperature. After
about one hour, a quasi-steady state evolution of the melt temperature is reached. At the time water is
poured on the melt, a rapid decrease of melt temperature is observed in some calculations.

![Ablation depth versus time](image1)

**Figure 4.4-2:** CCI-2 blind benchmark: Ablation depth versus time

![Final cavity contours](image2)

**Figure 4.4-3:** CCI-2 blind benchmark: Final cavity contours

The initial ablation rate differs depending on the code, and this discrepancy is connected to the
melt temperature behaviour. After about one hour, the ablation rates are less dispersed (Figure 4.4-2).
The final shape of the cavity (Figure 4.4-3) mainly depends on the choice made by the code user: either isotropic heat transfer (which corresponds to what was observed in the experiment) or radial
heat transfer higher than axial heat transfer.
4.4.2. The COMET-L2/L3 benchmark

The COMET-L2 test (Sdouz, et al., 2006) was performed at Karlsruhe Institute of Technology (KIT) in the frame of the LACOMERA project of the 5th European Framework Programme. The melt is composed of oxide (alumina and calcia) and metal (iron and nickel). A sustained heating power was released in the bottom metal layer for about 17 min. The COMET-L3 test (Alsmeyer, et al., 2007) differs from COMET-L2 mainly by a top flooding that was triggered at 13 min. After an initial transient period of about 100 s, characterised by an isotropic and fast ablation, the overheat of the metal is lost and a quasi-steady state regime is reached, with a faster axial ablation compared to the lateral ablation (factor 2 to 3), which is in agreement with the results of the BETA experiments at a low power density.

![Metal temperature versus time](image1)

![Oxide temperature versus time](image2)

![Axial ablation versus time](image3)

![Radial ablation versus time](image4)

**Figure 4.4-4:** COMET-L2 benchmark: CEA and IRSN presented a base calculation and calculations with a modified model for a better agreement with the experimental results.

The COMET-L2 test was used for a post-test benchmark. The participants were Areva with COSACO, CEA with TOLBIAC-ICB, EDF with TOLBIAC-ICB, FZK with WECHSL, GRS with ASTEC/MEDICIS and with WEX (which had been developed by GRS based on WECHSL), IRSN with ASTEC/MEDICIS and VTT with MELCOR. The same input data were used by all the participants. CEA and IRSN presented a base calculation and additional calculations with some modifications of the models in order to get a better agreement with the experimental results.

The scatter between the calculated metal temperatures in Figure 4.4-4 a) is about 150 K, but six results are between 1750 and 1780 K. The scatter between the oxide temperatures in Figure 4.4-4 b) is larger: about 350 K at 1000 s. There are no bulk temperature measurements for comparison. There
is also a large scatter concerning the ablation depth (Figure 4.4-4 c) and Figure 4.4-4 d), but it can be noticed that, after the first phase corresponding to the initial overheat, the ablation rate is similar for all the codes. Finally, when compared to the experimental results, it is found that the maximum axial ablation is underestimated.

The COMET-L3 test was used for a blind test benchmark. The participants were Areva with COSACO, CEA with TOLBIAC-ICB, EDF with TOLBIAC-ICB, FZK with WECHSL, GRS with ASTEC/MEDICIS and with WEX (which is derived from WECHSL), IRSN with ASTEC/MEDICIS, UPM with MELCOR and VTT with MELCOR. The experimental results were not known when the calculations were performed, but for some code, the model modifications tested in order to get a better agreement with COMET-L2 were used for the simulation of COMET-L3. The results are presented Figure 4.4-5(a)-(d).

The top flooding at 800 s is not really sensitive to the calculated results. It can be observed that the scatter of the calculated oxide and metal temperatures (Figure 4.4-5(a) and Figure 4.4-5(b)) is reduced compared to COMET-L2, because of fitting of some parameters to the results of COMET-L2. In the initial phase, some codes give a heat transfer from the oxide layer to the metal layer and the others from the metal to the oxide. In the second phase, before flooding, all codes predict heat transfer from the metal to the oxide. After flooding, some codes give back a heat transfer from the oxide to the metal.

For production of gas through oxidation of the metal layer (H2 and CO) the scatter is large, with about a factor 5 between the larger and the lower values. There is also a large scatter concerning the ablation depth (Figure 4.4-5(c) and Figure 4.4-5(d)). Some codes give results that are similar to the
Some others overestimate the axial ablation or the lateral ablation, and the ablated volume.

4.4.2.3. The VULCANO-VBU5/VBU6 benchmark

The results of two oxidic tests VB-U5 (silica-rich concrete) and VB-U6 (limestone-rich concrete) were used for a benchmark on MCCI codes as a part of the European Severe Accident Research Network of Excellence (SARNET). Ten participants from different European countries (CEA Cadarache France, CEA Grenoble France, IRSN France, GRS Germany, KIT Germany, VTT Finland, Areva Germany, El Bulgaria, INRNE Bulgaria, RSE Italy) took part in this work. Six computer codes (TOLBIAC-ICB, ASTECv2/MEDICIS, COSACO, CORQUENCH, WECHSL and CORIUM-2D) were used to perform two independent calculations, each one representing the main phenomena arising during the interaction between prototypic oxidic corium and siliceous or limestone concretes.

An example of the obtained final cavity shapes in case of the VB-U5 test is displayed in Figure 4.4-6. Clearly, codes imposing the anisotropy (by fitting a multiplicative factor applied to the convective heat transfer coefficients or imposing values of thermal resistances at pool/concrete interfaces) or using heat transfer models leading intrinsically to anisotropy permit to get a better agreement for the final cavity shape.

Figure 4.4-6: Crucible shapes at the end of calculation for VB-U5

Pool temperatures calculated for VB-U5 are shown in Figure 4.4-7. Most curves indicate a trend of monotonous decrease. In most of the calculations the pool temperature decreases faster during the first transient phase and this decrease slows down at later times; the only exceptions are TOLBIAC-ICB (CEA) and CORIUM-2D (RSE) calculations with the highest pool/crust interface temperature. It must be noted that the pool temperature measurement (2 403 K), which has been taken only during a small time period around 1200 s, is somehow overestimated as it has arisen from recent analysis of subsequent VULCANO experiments (the real temperature can be 30 to 200K lower). Only the TOLBIAC-ICB calculations which follow the liquidus temperature (Figure 4.4-7) give fairly good estimates of the high corium temperature measured.
Concerning the VB-U6 experiment with limestone rich concrete, similar fits have been observed. For instance, Figure 4.4-8 presents the experimental and computed cavity shapes.

Many similarities have been identified in the predicted trends. Nevertheless some major differences between modelling approaches were observed.

The ablated volume is controlled by the ablative energy, thus it is impacted by the amount of energy radiated through the upper surface which depends on the code heat transfer models (heat convection distribution and interface structure) and also on the interface temperature to the upper crust. Most codes overestimate the ablated concrete volume (based on a comparison of final melt composition data) if significant conduction heat losses through the concrete are not taken into account especially in case of VB-U6. Impact of the ablation occurring during the initial transient phase can also be important, in particular in experiments.

Cavity shapes are rather well predicted: the VB-U5 with siliceous concrete required taking into account anisotropy, either explicitly (with ASTECv2/MEDICIS, TOLBIAC-ICB) or implicitly (with CORQUENCH); axial ablation is generally overestimated.
• TOLBIAC-ICB calculations provide good estimates of the high pool temperature measured, whereas the other models give some discrepancies of several hundred kelvins at least in the initial MCCI phase of the VBU5 experiment. However, the calculated temperatures cannot be compared with the experimental one in the longer term since the overall pool temperature evolution was only measured at one unique time and the measured temperature may be significant only for the transient period of the experiment, during which validation of codes is a very ambitious task.

• All codes predict a small final void fraction (even with limestone test VBU6). The reason is probably that the drift flux model used to calculate the void fraction is not well applicable to 2D configurations, in particular in small scale experiments in which side area is greater than bottom area, contrary to the reactor case.

• Experimental results indicate that crusts, if they exist, are likely to have a composition close to the current pool composition. Therefore it unfirms the assumptions that crust could keep the same composition from the beginning (i.e. a time where there was less concrete in the pool) and that crusts contain a higher fraction of most refractory species. Such assumptions are in line with the TOLBIAC-ICB code hypotheses. Experimental observations show that crust formation, if any, is renewed all along the experiment and without any significant segregation.

4.5. Material properties validation status

It must be reminded that in this high temperature range material properties are known with significant uncertainties (Journeau, Piluso, & Frolov, 2004). Moreover, the codes are assuming that the melt is homogeneous or that there are two homogeneous melt phases (one oxide and one metal) whereas the VULCANO post-test analyses revealed the presence of concrete-rich and corium-rich plumes having different compositions and thus different properties, which cannot be taken into account with the current level of MCCI modelling.

Among the thermophysical properties, one of the uncertainties that may have the most consequences will be the density difference between oxidic and metallic phases. From the BALISE correlation, a 10% difference in the densities of phases can be mixed by a 0.5 cm/s sparging gas flow. The uncertainty on oxidic liquid density is clearly above 10% for complex melts: it depends on

• The knowledge of the phase composition.
• The knowledge of the partial molar volume of each constituents.
• The validity of the assumption of ideal mixing (no excess volume is considered).

Unfortunately, there are very few data available to validate these calculations.

Concerning the thermo-chemical properties (phase composition, liquidus temperature,…), ex-vessel corium experiments as VULCANO, EVAN, COMETA have been used to validate and improve the NUCLEA database (Bakardjieva, et al., 2010). Nevertheless, uncertainties of several wt% in composition \(1/10^{th}\) of the value for heavy elements, \(1/4^{th}\) for oxygen) and of more than 50 K in temperature are to be expected. A thorough analysis of the liquidus of EPR concretes showed that, in the vicinity of steep variations of liquidus temperature with respect to composition (300 K in a few wt%), the uncertainty on the liquidus temperature can be of the order of 300 K in this extreme case. Outside from these peculiar compositions, much lower uncertainties are expected which are consistent with the overall uncertainties of the codes.

For oxide-metal melts, uncertainties on the composition of phases at equilibrium may be significant, as the validation matrix is relatively scarce in MCCI compositions.
Finally, it must be reminded that thermodynamic equilibrium calculations assume that all phases are in equilibrium and do not account for any kinetic effects, which may not be totally the case. For instance, Scheil-Gulliver solidification has been found to explain better some of the compositions found in VULCANO samples better than equilibrium calculations (Bakardjieva, et al., 2010).

4.6. Scaling to reactor case

A discussion of remaining uncertainties resulting from code validation must be combined with uncertainties coming from the scaling to reactor case.

MCCI codes are validated on the basis of a range of experiments which were performed in test sections with a length scale $< 1$ compared to reactor scale. A very important question is in how far the codes’ models are also valid for predicting a large scale reactor scenario.

In this chapter higher-level code capabilities which can be tested in calculations for available integral experiments are identified. In the following it will be shown how far the available data of experiments may be evaluated for the long-term and large-scale reactor case.

4.6.1. On the mapping of experiments to reactor scale

With regard to the two basic features concrete ablation ($abl$) and melt temperature ($mt$) prediction during quasi-steady state, two parameters that may govern the physics for the MCCI are usually considered: i) the ratio of injected power divided by available surface area for cooling (this is the approximate heat flux density in quasi-steady-state) and ii) the ratio of injected power divided by the melt volume (this is e.g. a governing parameter for natural convection heat transfer). During the MCCI the variation of oxide melt properties is large. This is captured by tracking both the above mentioned parameters along the concrete weight fraction in the oxide melt, whereas for the steel melt the material properties will stay more or less the same.

Using a simplified procedure, which is described below, it is checked, how the design basis of some representative experiments would map to a typical MCCI reactor scale (using the volumetric internal power and/or the heat flux densities as scaling parameter) as function of concrete weight fraction.

Reference case is a core melt scenario in a German BWR with 156 t of UO$_2$ and 30 t of ZrO$_2$ in the oxide phase and 73 t of Fe, 50 t of Zr, 11 t of Cr and 6.4 t of Ni in the metal phase. This scenario was basis for a MCCI code benchmark in the SARNET project (Cranga, et al., 2010). After start of the MCCI (defined by $t = 0$ s) the decay power in the oxide and metal phases as function of time is shown in Figure 4.6-1.

![Figure 4.6-1: Decay power in the oxide and metal phases after start of the MCCI in the reference reactor case (Cranga, et al., 2010)](image-url)
The MCCI in a reactor may involve a sequence of different pool configurations due to segregation phenomena. Due to the non-miscibility of the oxidic and metallic liquids of which the corium is composed an establishment of different configurations can be expected depending on the strength of agitating effects like gas release from the decomposition of the concrete.

For simplicity the time evolution of typical heat flux densities at the surface of the corium and of the volumetric power are evaluated here assuming a static, homogeneous corium layer (metal dispersed in oxide melt). The required time-dependent data for this evaluation (i.e., surface area for cooling of the corium, volume of the corium, corium and concrete masses in the pool etc.) are taken from the ASTEC/MEDICIS BWR reactor benchmark calculation for the static homogenous pool configuration and the siliceous concrete as published in (Cranga, et al., 2010).

In Figure 4.6-2 the power and specific heat flux density calculated with ASTEC/MEDICIS for the reference BWR reactor scenario (Cranga, et al., 2010) are compared with estimated data of some representative experiments. The evaluation of volume and surface area of the pool obtained from ASTEC/MEDICIS includes the effect of voiding, whereas the experimental data are approximated for the collapsed corium. Focusing on specific heat flux densities (blue lines and blue symbols in Figure 4.6-2) the SURC experiments (e.g. SURC-1) represent a good approximation of the selected reactor case for the very early phase at low concrete fraction while the data of the CCI experiments represent a good approximation of the reactor case for the period up to 60 wt.-% concrete. Regarding volumetric power (red lines and symbols in Figure 4.6-2) the MACE-M3b experiment represent a good approximation of the period between 20 and 40 wt.-% concrete in the melt for this reactor scenario.

**Figure 4.6-2:** Mapping of experimental data of oxide experiments to a selected BWR reactor scale on the basis of an ASTEC/MEDICIS calculation (homogeneous case)

For a PWR scenario the overall melt mass will be smaller and the power density will be larger, thus a better agreement with experimental data for volumetric power is expected for a PWR.

Considering the processes within a stratified MCCI pool configuration including a segregated metal layer the situation is different: The composition of the steel melt will not vary strongly during the MCCI like in the case of the oxide melt. The volume will also not vary so much. Some amount of metal is leaving the layer due to oxidation, whereas some amount of iron is introduced into the layer by ablation of the reinforcement bars. Thus the volumetric power released in the metal and the specific heat flux density across the surface of the steel melt will not decrease as much as in the case of the oxide layer. Since there is no strong variation of material properties with concrete erosion which could
be applied to the case of the metal layer, the question remains of how to map experiments with steel melts to the reactor scale. There is no clear way to evaluate a scale for the segregated case.

Here, the following method was used to evaluate such a mapping: The transient evolution of the layer contour (surface area and volume of the metal layer) in the BWR reactor scenario is taken from the ASTEC/MEDICIS calculation for the stratified calculation case (Cranga, et al., 2010). For the estimation of power per volume and heat flux per area, however, the total decay power in oxide and metal layer is considered. This assumption is plausible because a focusing effect may redistribute a large part of decay heat of the oxide via the metal layer to the concrete.

Using this methodology it is found that, when regarding heat flux density at the corium/concrete interface, the BETAV5.2 experiment maps to the reactor calculation at the beginning of the interaction and the COMET-L3 experiment maps to the reactor calculation after some time of interaction (~ 0.5 days). However, the evaluation on the basis of the calculation with ASTEC/MEDICIS indicates smaller volumetric power levels compared to the design of available experiments. COMET-L3 may approximately represent an upper boundary of the volumetric power level in the metal layer at later stages of the selected BWR scenario (t ~ 2 days).

Although the agreement between calculated data and experimental data depends strongly on the selected reactor scenario and remaining uncertainties of code results (obtained here with ASTEC/MEDICIS) may be quite significant, the findings suggest that for the BWR scenario selected experimental data are principally available for the early phase of interaction, but data are missing for the very late phase (i.e., at large concrete fraction > 60 wt.-% in the oxide layer). This is in principle valid also for a PWR scenario. A further uncertainty for extrapolation of experiments to reactor scale is that in experiments with a segregated metal layer, the power was injected predominantly into the metal phase, which is not in agreement to the reactor situation (large fraction of power release in the oxide phase), except for the MOCKA experiments of KIT Karlsruhe, which are not fully examined yet and for the VULCANO experiments of CEA in which unexpected shape of the metallic pool is not yet explained.

4.6.2. Simplified transposition of important parameters from small to large scale

The exemplary study in the preceding section tries to map representative experiments to a reactor scenario considering the heat flux density (W/m²) to the concrete or the volumetric power (W/m^3) in the melt, respectively, and the concrete admixture to the corium as key parameters for scaling, which showed that experimental data cover the reactor scale up to approx. 60 wt.-% concrete (~ order of 1 day of interaction). This procedure was proposed on the basis of MCCI calculations with the MCCI code MEDICIS. Such MCCI codes tend to evolve into a sequence of quasi-steady states, in which the decay heat released in the melt is roughly redistributed into concrete melting and heat losses from the free surface. According to these quasi-steady states the level of the pool temperature is governed then by the efficiencies of heat transfer mechanisms and boundary conditions at the pool’s interfaces (expressed in terms of effective heat transfer coefficients and interface temperatures).

Because of that any scaling of the temperature history recorded during a small-scale MCCI to the reactor must take special care of the heat transfer between the melt and its interfaces. If this heat transfer explicitly depends on a geometrical dimension of the pool (e.g., on the characteristic length of the pool) then the temperature level of the MCCI will be scale-dependent. This may be the case if mechanisms such as natural or solutal convection govern heat transport.

The heat transfer is not affected by the geometrical dimension of the pool if bubble-induced forced convection is the driving mechanism for heat transfer in the pool: here the characteristic pool length itself does not appear in the heat transfer law, it is replaced by a characteristic bubble length (for example in the BALI heat transfer correlation which uses the Laplace constant in the definition of its Nusselt number).
Currently the mechanisms of heat transfer between melt and interfaces are not well understood and identified. Corium viscosity (at least in the boundary layer) and the gas release at the interface are suspected as parameters affecting the efficiency of heat transfer and/or the 2D heat flux distribution. Rough estimates for heat transfer coefficients and 2D heat transfer coefficient distributions for MCCI were obtained by empirical evaluations of MCCI experiments. A range of experiments can be recalculated with good agreement to experimental results applying these heat transfer coefficient estimates at the interfaces. It is presently not clear, whether these empirical data are invariant to scaling or not.

By properly selecting the geometry, the initial conditions and the heating power of a small-scale MCCI and under the assumption that heat transfer is invariant to scaling, the relevant variables as temperature, concrete erosion, melt composition of a large-scale MCCI system can simply be obtained by appropriate transformation from the small scale MCCI ((Fargette, 2013), (Spengler, Fargette, Foit, Agethen, & Cranga, 2013). However, some strong conditions for the corium pool geometry must be fulfilled.

For example, considering that one is interested in the temperature-history of the following (initially cylindrical) reactor-scale MCCI (basing on the BWR scenario considered in section 4.6.1): \( R = 3 \text{ m}, H = 1.8 \text{ m}, P = 20 \text{ MW} \). A small-scale MCCI experiment with the same initial melt temperature, composition and the following characteristics could be performed: \( r = 3 / 10 = 0.30 \text{ m}, h = 1.80 / 10 \text{ m} = 0.18 \text{ m}, p = 20 \times 10^6 / 10^2 = 200 \text{ kW} \) (assuming the heat flux density as one parameter which should be invariant to scaling). By stretching the initial temperature profile by the same factor 10, we then obtain the reactor-scale temperature profile, presumed that both systems evolve according to the constraint of ideal cylindrical geometry. In the example given the temperature measured at the test scale at \( t = 0.5 \text{ hr} \) corresponds to the reactor-scale temperature at \( t = 0.5 \times 10 = 5 \text{ hrs} \).

However, the condition of identical initial specific power density (W/m²) in both systems is not the only possible constraint for scaling. Under different scaling conditions, e. g. if the relevant variables are related by the ratio of the melt heat capacities, the time evolution of the melt temperature will be identical in both systems (Foit J. J., 2012).

A potential reason for the applicability of constant effective heat transfer coefficients at the interfaces to simulate MCCI experiments may be referred to the combined effect of cooling of the melt (\( \rightarrow \) increase of viscosity via increase of solid fraction with decreasing temperature) and dilution with concrete (\( \rightarrow \) decrease of viscosity via decrease of solid fraction with enrichment of concrete) which could result in a rather constant plateau of effective pool viscosity, dependent of thermo-dynamical data supplied (Spengler, 2013). More research efforts are required on this item: An improved knowledge of the heat transfer mechanisms at the pool interfaces is urgently required (but hard to gain) in order to confirm and/or replace the empirical effective heat transfer coefficients and cover potential effects of evolving MCCI conditions in the long term.

For a further confirmation of these findings additional theoretical studies are proposed: Reactor scale applications should be investigated thoroughly with reference to the time behaviour of the different terms in the energy equation, especially with regard to the validity of quasi-steady state assumptions (\( \rightarrow \) power redistribution to the interfaces, pool temperatures governed by effective heat transfer coefficients), and to the evolution and effect of more realistic cavity contours in contrast to simplified geometrical assumptions (cylinder, hemispherical section, etc.). This task could be fulfilled by a reactor benchmark with the contribution of several different MCCI codes.

4.7. Summary of the codes' validation status

From a general point of view a fully satisfying validation of the different terms in the complete energy balance in MCCI codes is not possible (see Section 4.1), since the quantities required are mostly not
available or inaccurate based on the documentation of available experiments, e. g. total energy used for ablation, total energy released from the free surface etc.

Because of that most codes have been validated against various MCCI tests primarily on the basis of maximum or average erosion depths and temperature history. Models show, that when the experiments temporarily run in a kind of quasi-steady state, i. e., at a smooth temperature evolution when the injected energy is substantially redistributed to the interfaces, a differentiated information could be obtained from that: The 2D heat flux distribution calculated in the codes is validated, if the propagation of erosion depths (or more precisely speaking: average erosion velocities) in 2D are well predicted for those quasi-steady time periods. If in addition the temperature of the melt is well predicted in the codes, this serves as a validation for the heat flux modelling \( q = h \Delta T \) in 2D for the quasi-steady time periods. The relation between the heat transfer coefficients \( h_{\text{top}}, h_{\text{side}}, h_{\text{bottom}} \) in combination with the imposed interface temperatures (concrete decomposition temperature, crust formation temperature for the top crust) governs the 2D heat flux distribution in the codes, and the magnitude of these heat transfer coefficients is a key parameter determining the long-term pool temperature.

Important progress in the understanding of 2D MCCI has been recently achieved in the frame of the SARNET research on MCCI (Cranga, et al., Towards an European consensus on possible causes of MCCI ablation anisotropy in oxidic pool, 2014): Good agreement between code calculations and experiments was obtained using effective heat transfer coefficients between the bulk of the corium and its interface in combination with the approved “melting model” for concrete erosion. Such effective heat transfer coefficients seem to be fairly constant throughout the experiments and are representative for the overall heat transfer from the bulk to the bottom and to the lateral pool interfaces, respectively.

The actual progress in the codes as outcome of the recent MCCI research may be summarised as follows: Experiments with oxidic melts confirm that total heat transfer coefficients (htc) between the liquid melt and the concrete ablation interface range from several 10 (in case of initial crusts) up to of a few 100 W/(m² K). Comparing this to empirical correlations for known convection mechanisms (natural, solutal or bubbly convection) these htc are rather small. The reason for such reduced htc is referred to thermal resistances developing at the interface between the melt and the concrete. Such thermal resistances may be interpreted as temporary crusts or highly viscous boundary layers. The detailed convection mechanism in the liquid bulk does not have large effect on the overall htc. The conditions controlling the stability of such interface structures in the long term seem to depend on the concrete type. In several experiments with siliceous concretes initial crusts at the bottom were of higher stability than at the sidewall. Since the presented MCCI codes do not yet feature any model for the simulation of crust stability under typical conditions, empirical htc have to be imposed by the code user. The research has shown that a local crust may or may not be present at the lateral or the axial interface. Consequently, the distribution of htc according to the interface angle and concrete type must be selected under the specific assumption that highly viscous thermal interface structures are present or not in the long term. The code user should be aware of this issue and should select a conservative parameter setting with view to the most crucial consequence for the specific accident sequence.

Taking benefit of this rather empirical methodology the code results show for many experiments an acceptable level of predictive capabilities for the estimation of concrete ablation (1D and 2D), particularly for homogenous pools with oxidic character and a limestone rich concrete. For the case of a homogeneous oxide melt and a siliceous concrete the anisotropic ablation observed in experiments is still related with a significant uncertainty with regard to quantification of the effect and to the mechanistic interpretation. Up to now the codes require empirical input parameters governing a non-isotropic 2D distribution of heat transfer for siliceous concretes, which may also be dependent on the local crust failure time.

It should however be stressed that there is still an uncertainty in the prediction of MCCI pool temperature levels. In the short term this is caused by insufficient knowledge of the material properties
during the initial temperature transient (i.e. evolution of latent heat etc.). In the long term this is most probably related to uncertainties in mechanistic modelling of the effective heat transfer coefficient distribution (i.e. the relation between \( h_{\text{top}} \), \( h_{\text{side}} \), \( h_{\text{bottom}} \)) and in the related interface temperatures, since the complex mechanistic and probably thermo-chemical processes contributing to the heat transfer are not known sufficiently. It is assumed that the corium viscosity in the boundary layer is one of the dominating properties governing the efficiency of heat transfer and thus controlling the temperature drop throughout the MCCI (see (Fargette, 2012), (Foit J. J., 1997), (Hackel & Gröll, 1969). A direct extrapolation of 2D heat flux distributions observed at a small scale to a larger scale (compare section 4.6.2) is still questionable as long as the mechanisms for the driving heat transfer through the interface structure are not well understood.

The transient processes during the incubation period in MCCI experiments are not sufficiently modelled in the codes. Several codes assume a transient crust at the corium/concrete interface which is a necessary feature to predict the behaviour of initial crusts. However, MCCI codes assume generally a conversion of heat flux into progression of ablation contour. To predict the phenomena during the incubation period correctly heat fluxes at the corium/concrete boundary should lead to heating of the concrete but – depending on the interface conditions – not necessarily to concrete erosion. Deepened theoretical efforts on this item would be of benefit for answering the question on the scaling issue with the incubation period.

With view to the phenomenon of gas release from the MCCI to the containment and oxidation reactions in the melt the validation status is lagging behind. This is one consequence of the intensified discussion on 2D ablation behaviour, since the gas release coming from the decomposition of the concrete is regarded only as subsequent effect of 2D concrete ablation and impacts directly the oxidation of metals. As soon as the discussion of 2D MCCI converges on a common interpretation (and modelling) of the 2D ablation, the validation of models should be continued with regard to the oxidation behaviour which will influence the composition of the gases released into the containment.

The validation of top flooding models and top crust formation in integral MCCI codes is not totally satisfactory in the different integral codes. The most comprehensive validation matrix for the phenomenon of top flooding was obtained with the code CORQUENCH (Farmer, 2010), including separate effect and integral tests. Some integral codes integrate a modelling similar to the CORQUENCH original models. Satisfactory validation on the available experimental database has been obtained with these models, see a.o. (Tourniaire, Boulin, & Haquet, 2015). In other codes than CORQUENCH the focus was not put on the top flooding/top crust formation issue until now, due to the priority of investigating the 2D dry MCCI issues first. Although these top flooding models are available, their application to large-scale MCCIs is questionable. These models were indeed derived from tests in which crust anchoring to the sides of the crucible affected water ingress and quenching. At a large scale, this phenomenon would not be present and a “floating crust” is expected. For the assessment of the top flooding/top crust effect on the course of the MCCI in experiments the crust anchoring effect has to be taken into account.

For the phenomenon of FP and aerosol release the richest data base is still represented by the large scale experiments of the international Advanced Containment Experiments (ACE) Program on melt behaviour and aerosol release during MCCI (Fink, Thompson, Armstrong, Spencer, & Sehgal, 1995). Model approaches for the fission product release by vapourisation are typically based on the application of thermodynamic equilibrium models of an ideal or non-ideal melt in contact with a gaseous phase. To calculate the thermodynamic equilibrium the Gibbs energy of the total system as a function of temperature and composition has to be evaluated. The vapourisation of fission products in the ex-vessel situation depends much on the temperature of the core melt during the interaction and on the gas flow rate coming from the concrete decomposition and since the uncertainties for the prediction of these quantities are still large the precision of calculated releases is – taking into account the inherent additional uncertainties for thermo- dynamical modelling – currently estimated to be at
least one or two orders of magnitude (Sehgal, Nuclear Safety in Light Water Reactors: Severe Accident Phenomenology, 2011). Mechanical aerosol generation by bursting of bubbles is not always considered in the codes. In that case an underestimation of aerosol release should be expected. A simple model for aerosol generation by bursting of bubbles is available in the VANESA code (Powers, Brockmann, & Shiver, 1986), which is part of CORCON and MELCOR, respectively.

Beside the clarification of 2D ablation phenomena further large uncertainties remaining are related to melt stratification and the impact of reinforcements on concrete ablation. Melt stratification criteria have only been derived from experiments based on simulants and an extrapolation to real melts remains questionable. The impact of reinforcements was not been thoroughly investigated in the past due to the inherent heating problems. These reinforcements might significantly impact the progression of the ablation front. Some new experimental data are available from the MOCKA test series at KIT Karlsruhe but they were not yet investigated thoroughly.

With regard to a potential transposition of the experimental data from laboratory scale to large scale, simple scaling laws were derived for a transposition of important parameters (e.g. corium temperature, maximum ablation depths, etc.) from small to large scale under ideal conditions, notably: homogeneous pool, retention of geometrical shape, restrictions on the variation of the heat transfer coefficient (e.g. scale-independency) and a simplified treatment of the power split. Simplified numerical approaches based on a simplified geometry to estimate important parameters are proposed which permit to check the overall consistency of results obtained with more detailed MCCI codes and which are useful for testing different MCCI modelling hypotheses based on bulk properties (e.g. potential impact of viscosity on heat transfer coefficient). A closer look at available MCCI experiments shows that experimental data are principally available for the early and mid-term phase of interaction, but data are missing for the very late phase (i.e., at large concrete fraction > 60 wt.-% in the oxide layer).

4.8. Conclusions

The available MCCI codes are validated on the basis of a broad range of experiments (see Table 4.3-2), featuring simulant and prototypic materials in different geometries and with different experimental heating devices. The focus of the validation work is commonly put on comparisons of the calculated results for corium temperature and local or maximum ablation depths with the experimental data.

Transient effects may have impact on the course of individual experiments – mostly at the start of the interaction between the newly generated melt with the structures – for which the codes cannot be assessed as “validated”. Such initial transients are related to the formation and stability of interface structures in the boundary layer of the melt. For the longer term however, experiments enter a quasi-stationary regime for which the code predictions on ablation progress and temperature history are well understood: The 2D power distribution in the codes is governed by the system of effective heat transfer coefficients in combination with imposed temperature conditions at the different interfaces. Good agreement with oxide experiments (as regards the ablation and temperature history) are obtained if htc in the order of 300 W/(m2K) are effectively used at the different melt/concrete interfaces in combination with the decomposition temperature of the concrete selected close to ~1 600 K as boundary condition to the concrete. Anisotropic ablation as observed for siliceous concretes can currently be captured in the codes only via imposition of anisotropic htc at the 2D interfaces.

Uncertainties are identified for melts consisting of oxides and metals: in a stratified configuration the thermal material properties of metal melt suggest elevated htc at the metal/concrete interface but the overall transfer of decay power (which is predominantly released in the oxide melt) to the concrete via the metal layer is finally governed by the heat transfer at the interface between the oxide and the metal layer. A direct model validation for this interlayer heat transfer is not yet currently possible due
to lack of appropriate data from experiments under MCCI conditions. Uncertainty is in addition introduced by insufficient knowledge of stratification/mixing processes under MCCI conditions.

The assessment of top flooding conditions on the course of the MCCI is not yet clear since the crust anchoring effect which is observed in experiments but not expected for the reactor scale is difficult to be taken into account.

The impact of concrete reinforcement on the MCCI is currently under investigation in the MOCKA experiments at KIT. In current reactor calculation, the chemical impact of iron rebars is considered (the reinforcement can be taken into account in the concrete composition), but not their structural impact.
5. **Plant applications**

5.1. **Introduction**

This chapter deals with the applications of MCCI phenomena, models, and data to safety analysis of nuclear power plants under severe accident conditions, particularly in the context of reactor safety requirements, and containment designs to address such requirements. Safety requirements are discussed first at a high level, promulgated by international bodies such as International Atomic Energy Agency (IAEA) and Western European Nuclear Regulators’ Association (WENRA), followed by requirements of national nuclear regulatory bodies such as US Nuclear Regulatory Commission (NRC), Japanese Nuclear Regulation Authority (NRA), French Nuclear Safety Authority (ASN) and German Nuclear Regulatory Authority.

Containment designs are discussed next for a number of generation II and generation III plants, specifically those design features that address the MCCI issue. Following the discussion of safety requirements and plant design features to address such requirements as well as the severe accident management strategies, three idealised plant (containment) configurations, used in the preparation of input for plant calculations, are discussed. A few example plant calculations are then presented.

This approach of plant idealisation is quite common and reasonable in the field of safety analysis, noting the inherent uncertainties in severe accident phenomena. As with virtually all other severe accident phenomena, MCCI has been investigated experimentally at a reduced scale using idealised geometries and, in some cases, using simulant reactor materials. Results from these experiments and other analytical approaches were used to develop models of core-concrete interactions, melt spreading, and debris coolability, both with and without water present in containment as a mitigation measure. Extrapolating the results of scaled experiments to MCCI in plant scale involves some idealisation of plant geometry and configuration.

5.2. **Plant safety requirements**

5.2.1. **IAEA safety requirements**

The International Atomic Energy Agency (IAEA), through its publication of IAEA Safety Standards series, promulgates general and specific safety requirements with regard to nuclear power plant design, operation, and maintenance. The specific publication, “Safety of Nuclear Power Plants: Design” (IAEA, 2012) establishes design requirements for safe operation of nuclear power plants, prevention of accidents that could compromise safety, and for mitigation of the consequences of such accidents. One of these requirements on the control of radioactive releases from the containment states that the design of the containment shall ensure that any release of radioactive material from the nuclear power plant to the environment is as low as reasonably achievable, and below acceptable limits under accident conditions.

The above requirement is pertinent to the MCCI issue as it means that consideration should be given to incorporating certain provisions into the plant design to enhance coolability of molten core debris, and mitigate the effects of core-concrete interactions. In particular, these provisions [discussed in (IAEA, 2004)] in more details are as follows:

- A means of flooding the reactor cavity with water to assist in the cooling process;
- Protection for the containment liner and other structural members, if necessary;
• Sufficient floor space on the basemat to spread core debris and to increase the capability of cooling the debris;
• A reinforced sump or cavity to retain molten core material debris; and
• Use of concrete type for the containment floor that minimises adverse effects due to molten core-concrete interactions.

5.2.2. WENRA safety requirements

The Western Europe Nuclear Regulators’ Association (WENRA) gathers representatives of the regulators from seventeen European countries and Switzerland with the main objective of developing a common approach for obtaining continuous improvement of safety. Due to its nature, WENRA does not aim to propose regulatory standards; instead, it proposes Safety Reference Levels with the purpose of harmonising safety practices that are linked to the IAEA Safety Standards. These representatives take part in working groups including a working group on reactor harmonisation (RHWG) that proposed statements on safety reference levels for existing reactors, including long term operation and safety objectives for new reactors, and when needed states positions on specific safety issues or on best practices that are part of safety evaluation processes such as PSA or PSR. Whereas a report on the harmonisation on safety at the European level was published in January 2011, the Fukushima situations led the WENRA to review approaches for both existing (WENRA, 2014) and new reactors (WENRA, 2013). These Safety Reference Levels have been reviewed by the members of the RHWG accounting for the revision of the Safety Standards made by the IAEA, for the results of the European Stress Tests and the European Nuclear Safety Regulators Group (ENSREG) peer review reports and for Safety Standards or Regulation reviews within national frames.

<table>
<thead>
<tr>
<th>Levels of defence in depth</th>
<th>Objective</th>
<th>Essential means</th>
<th>Associated plant condition categories (for explanation - not part of original table)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Level 1</td>
<td>Prevention of abnormal operation and failures</td>
<td>Conservative design and high quality in construction and operation</td>
<td>Normal operation</td>
</tr>
<tr>
<td>Level 2</td>
<td>Control of abnormal operation and detection of failures</td>
<td>Control, limiting and protection systems and other surveillance features</td>
<td>Anticipated operational occurrences</td>
</tr>
<tr>
<td>Level 3</td>
<td>Control of accident within the design basis</td>
<td>Engineered safety features and accident procedures</td>
<td>Design basis accidents (postulated single initiating events)</td>
</tr>
<tr>
<td>Level 4</td>
<td>Control of severe plant conditions, including prevention of accident progression and mitigation of the consequences of severe accidents</td>
<td>Complementary measures and accident management</td>
<td>Multiple failures</td>
</tr>
<tr>
<td>Level 5</td>
<td>Mitigation of radiological consequences of significant releases of radioactive material</td>
<td>Off-site emergency response</td>
<td>Severe accidents</td>
</tr>
</tbody>
</table>

Table 5.2-1: Refined description of levels of DiD from (WENRA, 2014)

For existing plants and specifically for LTO, WENRA recommends the safety objectives of new reactors be used as a reference for the definition through PSR of reasonable improvements that can be implemented in existing reactors. The report on Safety Standards for New Reactors (WENRA, 2013) reaffirms the concept of defence in depth (DiD) as the key concept that should be applied to all safety related activities and proposes to refine the description of the five levels of DiD to consider separately the control of accidents due to multiple failure events and to reinforce the separation between the consideration of accidents without and with significant core melt; see Table 5.2-1.
For new reactors, WENRA defined seven safety objectives. The third of these objectives is on Accident with Core Melt and specifies that these types of accidents should be included in the reactor design, and that the accidents that can lead to large or early releases should be practically eliminated, and for those events that are not prevented, they should lead to only limited time and area protective measures for the public. The fourth safety objective is on the independence of the levels of DiD; this objective implies that each Safety Feature should be diverse, structurally separated, and functionally isolated. The RHWG stated that to fulfil the third safety objective in level four situations, independent safety systems should focus particularly on containment integrity as the main protective barrier for the environment. These independent safety features should guarantee containment heat removal, control pressure rise including the effect of non-condensable gases, limit the risk of energetic events such as hydrogen combustion, and should avoid any uncontrolled leakage paths (through penetrations including melt-through, access hatches, and failure of liners). In addition, to limit the time and area consequences for the population, the inside containment source term must be lowered, containment venting must be efficiently filtered, and dedicated instrumentation must be developed as part of emergency preparedness. The RHWG recommends to analyse the adequacy of these accident progression and consequences mitigation features using combined deterministic (best-estimate) and probabilistic methods.

Adopting this fourth safety objective (specifically, separating safety features devoted to mitigation and preventing the progression of a core-melt accident) as a reference for existing plants has major implications for LTO (different cooling systems, electrical power supply, containment safety systems, IC cables, et al.).

5.2.3. NRC safety requirements

Safety and regulatory requirements for operating reactors (i.e. reactors of the Generation II type) in the United States are codified in Title 10 of the Code of Federal Regulations (CFR), in particular, Part 50 of the code and its various appendices. There are no particular regulatory requirements to address severe accident challenges. However, in the design process, the concept of defence-in-depth is incorporated which provides multiple physical barriers to the uncontrolled release of radioactive materials to the environment. Furthermore, the design and operation of a nuclear power plant must provide redundant means to ensure fundamental safety functions: reactivity control, heat removal, and confinement of radioactivity.

The safety requirements stipulate that consideration be given to severe accident sequences, using a combination of engineering judgement and probabilistic methods, to assess severe accident challenges and identify preventive and/or mitigation measures. Acceptable measures need not involve the application of conservative engineering practices used in setting and evaluating design basis accidents, but rather should be based upon realistic or best estimate assumptions, methods and analytical criteria.

The current design safety requirements are met by ensuring that structures, systems or components perform the following safety functions in a reliable manner:

- Prevent uncontrolled reactivity transients;
- Maintain the reactor in a safe shutdown condition after all shutdown actions;
- Maintain sufficient reactor coolant inventory for core cooling in and after accident conditions not involving the failure of the reactor coolant pressure boundary;
- Remove heat from the core after a failure of the reactor coolant pressure boundary in order to limit fuel damage; and
- Transfer heat from safety systems to the ultimate heat sink.
Since TMI-2, the US Nuclear Regulatory Commission (NRC) issued a number of severe accident policy statements aimed at ensuring that the severe accident challenges to the current fleet of reactors are appropriately managed by the industry and that any new reactor design (generation III varieties or advanced) is as safe as or safer than the Generation II varieties with regard to severe accident challenges. In response, the industry initiated the development and implementation of severe accident management (SAM) strategies. This is a voluntary initiative by the industry with no regulatory oversight requirements. However, the industry is required to keep NRC informed of the status of SAM activities on a regular basis.

In the unlikely event of a severe accident in which the core melts through the reactor vessel, it is possible that containment integrity could be breached if the molten core is not sufficiently cooled. SECY-90-016 (NRC, 1990) recommended criteria for evolutionary LWR designs to provide sufficient reactor cavity floor space to enhance debris spreading, and provide for quenching debris in the reactor cavity.

The Electric Power Research Institute (EPRI) requirements document provides floor sizing criteria of 0.02 m²/MWt, and provisions to flood the lower drywell or reactor cavity. While this is not a regulatory requirement, the design should ensure that the containment will accommodate the consequences of core-concrete interactions (i.e. basemat ablation not to exceed the limit that will challenge containment integrity and gas production not to exceed the amount that will over-pressurise the containment) for approximately 24 hours.

In SECY-93-087 (NRC, 1993), the above provisions were formalised as requirements for the evolutionary and passive LWR designs to meet the following criteria:

- Provide reactor cavity floor space to enhance debris spreading;
- Provide a means to flood the reactor cavity to assist in the cooling process;
- Protect the containment liner and other structural members with concrete, if necessary;
- Ensure that the best estimate environmental conditions (pressure and temperature) resulting from core-concrete interactions do not exceed Service Level C for steel containments or Factored Load Category for concrete containments, for approximately 24 hours; and
- Ensure that the containment capability has margin to accommodate uncertainties in the environmental conditions from core-concrete interactions.

Since the Fukushima accident in March 2011, there is a renewed debate on the need to provide a regulatory framework and oversight for beyond design basis accidents. The Near Term Task Force, put together in the aftermath of the Fukushima accident, recommended that the Commission direct the staff to initiate actions to enhance the NRC regulatory framework to encompass beyond-design-basis events (including severe accidents) and their oversight (NRC, 2013). These actions included issuance of orders and initiation of rulemakings. Work is currently in progress on the mitigation of beyond-design-basis events (MBDBE) rulemaking. The proposed MBDBE rulemaking has several provisions one of which is to make generically-applicable previously imposed requirements for the mitigation of beyond-design-basis external events by Order EA-12-049 (NRC, 2012).

The MBDBE rulemaking is not expected to alter the provisions of SECY-93-087 (NRC, 1993) with regard to the MCCI issue. However, in light of the lessons learnt from the Fukushima experience, it may be necessary to address much longer transients and hence, containment behaviour during longer duration MCCI.

### 5.2.4. Japanese safety requirements

Through severe accidents at TEPCO’s Fukushima Daiichi Nuclear Power Station, triggered by the devastating natural events of March 2011, many important lessons on nuclear safety issues were
learnt. As one of the post-Fukushima actions, improvement of nuclear safety management and regulation has been promoted by a new nuclear regulatory body, the Nuclear Regulation Authority (NRA). As an output of a complete review of regulatory requirements and safety guidelines with the aim of formulating a set of new regulations to protect people and the environment, new regulatory requirements went into effect on 8 July 2013 that are being back fitted to operating commercial power reactors.

Based on a concept of “Defence-in-Depth”, essential importance was placed on the third and fourth layers of defence and the prevention of simultaneous loss of all safety functions due to common causes such as earthquakes, tsunamis and other external events such as volcanic eruptions, tornadoes or forest fires. Furthermore, countermeasures are required against internal fires and internal flooding, and to enhance the reliability of on-site and off-site power sources to deal with the possibility of station blackout (SBOs). In addition to the above-described countermeasures established at design basis, it is also required to establish countermeasures for severe accident response against core damage and containment vessel failure.

In view of MCCI, it is required to prepare equipment and procedures for cooling molten core material relocated from the reactor pressure vessel (RPV) into the containment vessel in order to prevent containment vessel failure in the event of severe core damage. The purpose of cooling of the molten core material relocated to the bottom of the containment vessel is to mitigate the molten core and concrete interaction (MCCI) and prevent the spread and contact of the molten core with the containment vessel boundary.

“Equipment and procedures for cooling the molten core fallen to the bottom of the containment vessel” should meet the following conditions in addition to other conditions that are commonly required for SA measures. Those measures that have the same or improved effect can be adopted.

- Equipment for injecting water into the bottom of the containment shall be prepared.
- Equipment for injecting water into the bottom of the containment shall be redundant or diversified, independent and dispersed in different locations.
- Relevant equipment shall be connected to alternative power sources.

5.2.5. German safety requirements

The revised German “Safety Requirements for Nuclear Power Plants” (BFS, 2012) contain fundamental and general safety-related requirements within the framework of the non-mandatory safety standards and rules that should be considered to prevent any damage caused by the operation of the plant in agreement with the regulations of the Atomic Energy Act (AtG). Regarding the nuclear power plants operated in Germany, these requirements have to be considered for modification licences. The German safety requirements are general in nature and do not provide any regulation specific to the consequences of MCCI. In the following these regulations are outlined, which generally concern the phenomena and consequences of MCCI.

The superordinate objective is to ensure the confinement of the radioactive materials present in the nuclear power plant and the shielding of the radiation emanating from them. In order to achieve this objective, a “defence-in-depth” safety concept shall be implemented in which measures and equipment are allocated to different levels of defence. Measures and equipment for internal accident management shall be provided and planned for supplement levels of defence 4b and 4c of the defence-in-depth concept. These levels of defence are characterised by the following plant conditions:

- Level of defence 4b: events involving the multiple failure of safety equipment.
- Level of defence 4c: accidents involving severe damage to fuel assemblies.
For level 4c accidents, mitigative internal accident management measures shall be provided for the purpose of maintaining – by using all available measures and equipment – the integrity of the containment for as long as possible, excluding or limiting releases of radioactive materials into the environment, and achieving a long-term controllable plant state. Furthermore, measures shall be planned to support external accident management in order to assess the consequences of accidents with potential or actual releases of nuclear materials into the environment, and to mitigate as far as possible their effects on man and the environment.

Taking into account both internal and external accident management planning, then any release of radioactive material into the environment caused by the early failure or bypass of the containment shall be excluded, or their radiological consequences shall be limited so that external accident management will only be required to a limited spatial and temporal extent.

In order to fulfil these high level requirements, it is mandatory that the MCCI does not cause or contribute to large and/or early releases on a technical level. This means:

- the gaseous release from MCCI must not threaten the containment by pressure build-up; if the containment load limit could be reached, adequate counter measures have to be implemented (e.g. filtered venting).
- the erosion of the basemat must not lead to early containment failure, taking into account also local inhomogeneities (cavities or penetrations) in the sump area.

With a view towards potential MCCI scenarios, the safety requirements demand that depressurisation of the primary circuit be effectively carried out so that there will be no core meltdown under high pressure. This means that melt ejection from the RPV under high pressure will not be probable, so that a redistribution of melt to other containment rooms other than the reactor cavity will be unlikely.

For the design of mitigating measures for internal accident management on level of defence 4c, a spectrum of events shall be postulated that takes into account all relevant phenomena for accidents that involve core melting. In this context, special attention shall be paid to those phenomena that are a risk for containment integrity. Furthermore, the phenomena that have an effect on the release of radioactive materials and on possible release paths to the environment shall be considered. If the present accident management measures implemented prove to be ineffective, additional severe accident management guidelines shall be provided for the emergency response staff. The suitability of the severe accident management guidelines for achieving the fundamental safety functions shall be demonstrated.

Potential measures to flood the ex-vessel core-melt with water are currently under focus as part of the considerations for developing SAMGs for individual plants.

5.2.6. French safety requirements

France has only one nuclear utility, Électricité de France (EDF), that is operating a fleet of 58 standardised pressurised water reactors (PWRs) (3 series of 900, 1,300 and 1,450 MWe). Similar to other Generation II reactors, severe accidents were not considered in their original design. However, Periodic Safety Reviews (PSRs) conducted every 10 years have led to the design, assessment, and implementation by EDF of significant plant modifications to include SAM equipment such as Passive autocatalytic hydrogen recombiners (PARs), Emergency Filtered Containment Venting System (EFCVS), containment strengthening, dedicated instrumentation (Cénérino, et al., 2016), and SAMG.

A PSR is carried out for the whole reactor series considered. Associated studies must be completed early enough so that modifications to equipment and documents can be deployed on reactors from the start of their ten-year outage period. The 3rd PSR for the 900 MWe PWRs (34 reactors) ended in 2008-2009 and their ten-year outage period is ongoing. Their 4th PSR started in
2014 as their 4th outage period is planned from 2019 to 2029. The 3rd PSR for the 1 300 MWe PWRs (20 reactors) is currently being finalised with their 3rd outage period planned from 2015 to 2021.

From 2009, the French Safety Authority (ASN) with the support of its technical organisation, the IRSN, started the evaluation of the strategy proposed by EDF for the Plant Lifetime Extension (PLE) from 40 to 60 years within the specific context of the construction on the Flamanville site of a first EPR of the generation III (or III+) type. This EPR is close to two Gen II 1 300 MWe PWRs that have been operating since the mid-eighties. Consistent with the position of WENRA, the ASN required EDF to refer to the safety objectives of the generation III reactors for all the safety studies made for the Generation II PLE. Concerning severe accidents, these safety objectives for generation III reactors including the EPR should lead to “only very limited protective measures in area and time for the public”. Thus, the proposition made by EDF does not only focus on the ageing management of Safety Systems and Components (SSC), but also includes a safety enhancement programme in which EDF intends to examine the possibility of implementing measures that includes the improvement of the EFCVS efficiency, the improvement of the containment decay heat removal without opening the EFCVS, and measures to avoid corium basement melt-through in case of RPV failure. The French regulatory framework does not limit the duration of operation for nuclear facilities and propositions made by EDF for PLE are evaluated plant by plant, in the frame of their 4th PSR.

In addition, post-Fukushima complementary safety evaluations (CSE) in 2012 led to the statements ECS-ND1 and ECS ND16 of resolution 2014-DC-0403 (ASN, 2014) by ASN that also requires EDF to investigate the possibility of improving containment heat removal systems and implementing measures to avoid basement melt-through in case of MCCI as part of a set of measures called “hardened safety. Solutions proposed by EDF will be evaluated in the CSE context plant by plant. In the framework of the 3rd PSR EDF has been granted with a ten-year extension for the Fessenheim plants (two 900 MWe PWR operated since the late-seventies) with a specific requirement to deal with their thinner reactor basemats. In answer to this ASN requirement, EDF has implemented in 2013 significant modifications that includes an ex- vessel corium spreading surface area extension and thickening. It is described in the section presenting the French PWR containment characteristics to highlight the differences that can exist within the implementation of concepts for a given reactor type. Finally, presently, both PLE and CSE proposals by EDF are examined in the frame of 4th PSR of 900 MWe reactors.

5.3. Reactor containment designs

Containment designs not only differ between reactor concepts, but also between different implementations for the same reactor type. These differences arise from the implementation of safety and regulatory requirements of the country or region where the plant was built. Differences relate to the presence of drains or sumps, specific seismic risks, or specific geologic configurations that lead to constraints on foundations, flooding risk prevention, and from the type of concrete components (mortar, cement, aggregates) available close to the plant location, among others. These differences must be accounted for when evaluating basement melt-through risks. It is not the purpose of this section to extensively review different implementations, but to present some of these concepts and indicate general features that may need to be addressed as part of plant-specific MCCI analyses.

5.3.1. Examples of generation II reactors

5.3.1.1. PWRs operated in France

In French 900 MWe PWRs, the containment building is made of reinforced concrete with a steel liner. The surface of the reactor pit is a keyhole geometry of about 30 m² surrounded by vertical concrete walls, see Figure 5.3-3. An access corridor is located ~1 m above the surface of the basement. The core instrumentation room (~50 m²) is separated from the reactor cavity by a vertical concrete wall of ~ 1.5 m thickness. This room is at the same level as the reactor cavity.
The containment of the French 1300/1450 MWe is ensured by two concrete vessels. The outer one, designed to withstand external aggressions, is made of reinforced concrete and the inner one is made of pre-stressed concrete; see the right part of Figure 5.3-2. The access corridor of the reactor pit is at the same level as the basemat, or slightly above this level (depending on the sites). The total surface (corridor + reactor cavity) is roughly 42-47 m². Similarly to French 900MWe PWRs, the reactor pit is located in the centre of the reactor building. Therefore, MCCI studies focus mainly on axial basemat penetration.

Basemat design and thicknesses (see Figure 5.3-2) and basemat concrete type depends on the plants site. Most of the 900MWe PWRs have a basemat thickness of around 4.7 m whereas the basemats of the 1 300MWe and 1 450MWe PWRs range between 3.1 m and 3.6 m thick. 900MWe CP0 plants located near Fessenheim were designed with a thinner basemat of 1.5 m. 900MWe CP0 plants located near Bugey were designed with two successive concrete slabs. Aside from the reactor cavity, some other compartments exist. Most of the plant basemats are made using siliceous-rich concrete whereas the plants in 5 locations have been built using LCS concrete.

**Figure 5.3-1:** Generic containment designs. Right: PWR900. Left: PWR1300 (credit IRSN)

**Figure 5.3-2:** Containment sectional views: from Left to Right PWR900-CP0 Fessenheim, PWR900-CP0 Bugey, PWR900-CP, PWR1300MWe-P4 and PWR1450MWe-N4 (credit IRSN)
As mentioned in section 5.2.6, in the framework of the 3rd PSR EDF had been requested by the ASN to significantly reduce the risk associated with basemat melt-through before being granted a 10 years lifetime extension of the Fessenheim plant. EDF proposed very significant modifications that have been evaluated by the ASN with the support of IRSN and considered to be satisfactory. The implemented modification is a thickening of the basemat of both the reactor cavity and an adjacent room with a 0.5 m thick layer of self-levelling LCS concrete; see Figure 5.3-4. The reactor cavity has been linked to this adjacent room using a transfer channel including a concrete fusible plug. In this adjacent room, concrete vertical walls have been built to prevent spray and sump water to fill the dry surface area devoted to corium spreading. These modifications allow increasing significantly the basemat melt-through delay in dry condition and, contrary to the initially designed configuration, no accident scenario has been found with the current state of knowledge on MCCI that can lead to a basemat melt-through within the first 24 hours after the accident starts; the period needed in France to implement the first population protection measures.

Figure 5.3-3: Typical EDF PWR900 reactor cavity (ref: DES180) Left: side view; Right: top view (credit IRSN)

Figure 5.3-4: Modifications implemented for the Fessenheim plants (ASN)
One can mention that to avoid any basemat melt-trough, the corium should spread on the whole dry surface areas before cooling water is injected on top; this illustrates the tight link between the MCCI issue and the water management issue. As an example of the possibility to flood the reactor pit before vessel failure, one can mention for PWRs, the fact that after the inner containment spray activation, the reactor cavity could be filled with water up to the RCS pipe level within a short delay of 1.5 to 2.5 hours. In spite of a flooded pit, if the reactor vessel fails the corium spreading on the basemat will be affected and the possibility of an intense steam explosion has to be considered.

Considering these scenarios, the recent IRSN stand is that the reactor cavity should be kept dry until the vessel lower head fails. This eliminates the risk of a large early radioactive release due to FCI and also increases the chances to maintain the functionality of the structure, system and components (SSCs) needed for SAM after vessel failure (e.g. water injection systems), as well as the chances for the corium to spread over a larger area and then be cooled by water injection from the top as soon as the water injection systems are recovered. This issue is being considered for NPPs as part of the long-term operation safety evaluation process. Consequently, EDF proposals are expected for the 900 MW(e) series in the framework of their 4th PSR.

5.3.1.2. US BWR

Mark I primary containments consist of an inverted light bulb-shaped drywell vessel surrounded at the base by a torus-shaped pressure suppression chamber, as shown in Figure 5.3-5. The drywell and suppression chamber are connected by vent lines (typically 8 equally spaced lines around the base of the drywell).

Figure 5.3-5: BWR Mark I containment design (3D cutaway and containment simplified schematics from (NRC, 2011)
The Mark II containment design retains the basic pressure suppression function of the Mark I containment, but rearranges the drywell and the suppression chamber into an "over/under" single pressure vessel configuration as illustrated in Figure 5.3-6. The vessel is supported by a concrete basemat in which the bottom of the vessel is embedded. The drywell and pressure suppression chamber are separated by a diaphragm slab. Vertical downcomers connect the two volumes. Steam released into the drywell during a LOCA is directed into the downcomers and is discharged below water level in the suppression chamber. The Mark II containment design has been implemented using three different construction techniques: (1) free-standing steel, (2) reinforced concrete with steel liner, and (3) post-tensioned concrete with steel liner. Mark II plants also have different pedestal region geometries. These are shown in Figure 5.3-7.

The Mark III containment, shown in Figure 5.3-8, is substantially larger than either the Mark I or II vessels and houses nearly all the reactor building components. The Mark III containment consists of
a drywell and pressure suppression chamber inside a primary containment shell that is surrounded by an enclosure or shield building and various equipment rooms that function as part of the secondary containment boundary. The Mark III containment design, Figure 5.3-9, has been implemented using two construction techniques: (1) free-standing steel, and (2) reinforced concrete with steel liner.
Section 5.5. Also, there is extensive below-vessel structure in BWRs consisting of control rod drive (CRD) and in-core instrument (ICI) tube penetrations. This structure can hold up core melt (by virtue of the heat sink) as well as cause a distributed melt pour condition that would be more amendable to quenching if water is present on the drywell floor. This occurrence can significantly alter the initial conditions for the core-concrete interaction phase of the accident.

As noted previously, in the Mark I containment design the steel drywell shell that forms the pressure boundary is also in relatively close proximity to the reactor vessel, and so direct melt contact of this structure can pose a threat to containment. For the Mark II containments, the downcomers are located on the drywell floor, and contact with these structures by melt during core-concrete interaction may cause localised failures, thereby providing a pathway for melt to relocate to the suppression pool. However, the suppression pool in the Mark II design is inside containment and so this behaviour would not bypass containment.

GE BWR plants are deployed across the continental US. The basemats in these plants are thus constructed from an array of concrete types ranging from Limestone/Common Sand, siliceous, to basalt.

5.3.1.3. German PWR of Type “Konvoi” description

In the German PWR of type Konvoi the overall thickness of the concrete foundation is approximately 6 m. This thickness will be eroded during MCCI after several days of interaction, if MCCI is not terminated by the overlaying water pool (see Figure 5.3-10). However, there are two specific details which require special consideration within an analysis of a severe accident with MCCI. It is assumed that these details are representative of features which may be found in similar plant designs.

![Cross-sectional view (schematic) of the lower part of the Konvoi containment](GRS, 2002)

Figure 5.3-10: Cross-sectional view (schematic) of the lower part of the Konvoi containment (GRS, 2002)

At floor level within the cylindrical concrete support structure for the RPV (outer border of reactor cavity), there are 8 pressure equalisation flaps (see Figure 5.3-10) which will open in case of pressure differences developing during an accident. Consequently the water from the containment sump will flow into the annulus between the RPV support structure and the biological shield. About 0.7 m below the bottom of the cavity there are ventilation ducts in a star-like pattern to remove heat from the concrete structures. The ducts are connected to openings at the bottom of the annulus between the RPV concrete support structure and the biological shield (primary or inner boundary of reactor cavity), so that water will enter the ventilation ducts as well.
One possible scenario identified within a PSA level 2 study (GRS, 2002) is characterised as follows: When the melt erodes the concrete and penetrates into this network of ducts, melt will flow towards the containment sump. After a failure of the steel ducts (rising vertically at the inside of the containment sump) the melt will reach the sump region where it can spread over an area of approx. 200 m². Another scenario, which is possible, is dominated by a first dry MCCI phase and a radial erosion of the biological shield until its failure. Thereafter, water ingestion is possible as well as melt release into annulus and sump and MCCI may continue. It is important for the further event progression whether the melt will reach the suction pipe stubs of the emergency core cooling systems. When the protection tube of the suction pipe develops a leak, for example due to melt impact, there will be a containment leak. Furthermore, small melt mass flows due to settling of melt particles generated after melt/water contact could also accumulate significant amounts of core material in the inside of the sump suction pipes with the potential consequence of a containment failure.

The second detail is that in the bottom of the sump of the containment there are cavities which provide some amount of free volume at the deepest level of the sump to collect remaining water for drainage by pumps. Below these cavities there is only ~ 1 m of concrete left until the melt would reach the steel liner of the containment. If melt fills up these special cavities the local thickness of the heat generating melt will be higher than what is generally considered as coolable under flooded conditions and under the 1D assumption of an infinitely extended melt layer, so that the coolability under top flooding conditions is challenged in these spots.

5.3.2. Example of generation III reactors

5.3.2.1. EPR™

For stabilising the melt in a severe accident, the EPR™ relies on an ex-vessel core catcher located in a lateral compartment below the reactor pit (see Figure 5.3-11). Thanks to the spatial separation between pit and spreading compartment, the core-catcher is safe from potentially critical loads related to the failure of the RPV. Furthermore, an unintentional flooding of the core catcher during power operation does not affect the safety of the plant. As a consequence of this uncoupling, power operation and design-basis mitigation measures remain unaffected by the existence of the core catcher.

The melt relocation into the core catcher is preceded by a phase of temporary melt retention in the pit, which addresses the prediction that the release of molten material from the RPV will likely take place in several pours. Temporary retention is achieved by a sacrificial concrete layer. Its ablation and incorporation leads to predictable melt characteristics that are independent of the preceding accident scenario. Ablation of the sacrificial concrete ultimately exposes the melt gate in the centre of the pit bottom. After the gate fails, the accumulated melt will spread on the large inner surface of the core catcher and its depth is expected to be lowered to the point where it would be coolable by conduction.

Both the reactor pit and the core catcher are initially dry to eliminate the risk of energetic steam explosion that could cause early containment failure. But the melt arrival thermally destroys triggers that open passive flooding valves and initiates the gravity-driven inflow of water from the Internal Refuelling Water Storage Tank (IRWST). The water quickly submerges the outside of the core catcher and eventually floods the melt’s free surface. Decay power is extracted by evaporation and steam release into the containment. The condensate flows back into the IRWST from where it is resupplied by passive overflow. As an option, the Containment Heat Removal System (CHRS) can be used to actively provide water to the core catcher. This will completely submerge the spreading compartment and the reactor pit and stop further steam discharge into the containment as a pre-condition to reach atmospheric pressure conditions in the long-term, without the need of a venting system.

The provision of the core-melt stabilisation system (CMSS) avoids the interaction of the molten core with the structural concrete and with it the risk of i) penetrating the embedded liner,
ii) weakening and mechanical deformation of load-bearing structures as well as the basemat itself, and
iii) sustained release of non-condensable gas into the containment atmosphere.

The melt stabilisation process involves the following stages (see Figure 5.3-11):

- RPV failure and initial melt release;
- Temporary melt retention and accumulation in the pit;
- Opening of the gate and melt spreading;
- Flooding and quenching of the spread melt;
- Long term cooling and heat removal to the water.

![Diagram of EPR core melt stabilisation system (CMSS)](image)

**Figure 5.3-11:** Main components of the EPR core melt stabilisation system (CMSS) (Fischer, *Long-term melt stabilization as part of the severe accident mitigation strategy of the European Pressurized Reactor (EPR), 2005*)

During this sequence, a transformation of the molten corium into a coolable and cooled configuration is achieved on the basis of simple physics and without requiring further operator action or the use of internal or external active systems and by using existing technology and materials that are commonly available and applied also in other industrial areas.
5.3.2.2. ESBWR

The ESBWR design includes a device called the Basemat Internal Melt Arrest and Coolability (BIMAC) device that is intended to arrest core melt progression in the lower drywell by cooling the debris both from above and below. The BIMAC device is a passively cooled barrier to core debris on the lower drywell floor. The design features a series of side-by-side inclined pipes, forming a jacket, which is passively cooled by natural circulation when subjected to thermal loading. Water from the Gravity-Driven Cooling System (GDCS) pools enters the pipes via connecting downcomers. Once the pipes fill up, the debris is also cooled from above from water that flows out of them. The timing and flows are such that the cooling by natural circulation becomes functional immediately upon actuation, and cooling by top flooding takes effect subsequently. The design procedure of not immediately adding water greatly reduces the probability of an energetic steam explosion.

5.3.2.3. EU-ABWR

The EU-ABWR design includes mitigative features against MCCI, consistent with the guidance in SECY-93-087. The most important features are: a large lower drywell floor area with minimal obstructions to the spreading of core debris; a lower drywell flooder system (active or passive); and a sacrificial basaltic concrete layer for the lower drywell floor. The optional design for installing the core catcher on the lower drywell floor is proposed to exclude any MCCI effects. The core catcher consists of a round basin to capture the ejected core melt, with lower inclined cooling channels axisymmetrically arranged, an annulus riser, an annulus downcomer and a central water chamber, and the passive flooder system. After core melt ejection, the fusible valves of the passive flooder open, and the suppression pool water flows into the peripheral annulus downcomer of the core catcher. After core catcher flooding, natural circulation is established within the cooling channels and downcomer, and the core melt is cooled passively from the top by the flooding water and from the bottom by the lower inclined cooling channels (Suzuki, Tahara, Kurita, Hamazaki, & Morooka, 2009).
5.3.2.4. **AP1000**

The AP1000 design employs the concept of in-vessel retention by external cooling of the lower head with a cavity flooding system to prevent vessel failure and consequent relocation of core debris in the cavity and any potential MCCI. Nevertheless, in case of vessel failure, the cavity design includes a thick layer of concrete to protect the embedded containment shell with an additional thick concrete layer below the liner elevation. The AP1000 design also relies on safety grade RCS depressurisation and incorporates plant features to promote debris spreading, consistent with the guidance in SECY-93-087 regarding debris coolability.

5.3.2.5. **APWR**

The US-APWR design includes a large area in the reactor cavity to provide floor space for debris spreading and quenching capability to cool the debris. The design would provide retention and long-term stabilisation of the molten core debris inside the containment. The melt is cooled by cavity flooding using two independent means: (1) containment spray utilising water from the in-containment reactor water storage pit (RWSP), and (2) fire water. Flooding of the reactor cavity occurs by manual initiation of the system when core damage is detected, provided the water is below a certain level. Since the US-APWR is designed to fill the reactor cavity with water when a severe accident occurs, external reactor vessel cooling may be also possible. However, in-vessel retention is not credited for the US-APWR in the Level 2 PRA study due to its inherent uncertainty.

5.3.2.6. **VVER-1000**

A crucible-type core-catcher has been designed for the reactor pit of the AES91 model (Khabensky, Granosky, & Bechta, 2009) of the VVER-1000 series of reactors and for its VVER-1200 evolution (Zvonarev, et al., 2011). This core-catcher device relies on a steel vessel located directly below the vessel that is externally cooled by water; the lower head has a conical shape to take benefit from the critical heat flux increase similar to the ESBWR and US-ABWR core catcher concepts (see for example (Suzuki, Tahara, Kurita, Hamazaki, & Morooka, 2009). A sophisticated honeycomb sacrificial structure fills the main part of the core-catcher vessel where each steel cell contains oxidic materials (see Figure 5.3-14).
The design of this complex sacrificial structure has been defined to meet specific requirements related to durability during the course of normal plant operations, the limitation of the steam explosion risk in case of unexpected early flooding, the stability of the solidified melt configuration after the accident progression has been terminated regardless of the coolant conditions, and finally the ease and safety of the decommissioning operation. The oxidic sacrificial material blocks contained inside the steel cells have been selected mainly to guarantee a fast inversion of the metallic and oxidic layers, thereby increasing the volume of oxidic materials (this lowers the heat flux due to residual power dilution and risk of recriticality), to limit the hydrogen generation and, similar to the EPRTM, to limit the uncertainties related to melt composition for all possible accident scenarios. For the AES91 concept, the oxidic sacrificial composition is approximately 70% Fe2O3, 30% Al2O3, 5% SiO2 and 0.1% Gd2O3. After a delay period evaluated to be sufficient for the two layer configuration with the oxide above the metal to be achieved (see (Zvonarev, et al., 2011) for an illustration of this evolution through simulation results), the melt is flooded by water.

5.3.2.7. **EU-APR1400**

A core-catcher has also been designed for the EU-APR1400 version of the APR-1400 PWR reactor developed by KEPCO in South Korea. This version of the APR-1400 includes a set of SAM devices and measures added to satisfy specific safety requirements promoted in European countries and especially the European Utility Requirements (EUR Organisation, 2012). Among these devices, the Passive Ex-vessel corium retaining and cooling system (PECS) described on Figure 5.3-15 is based on a core-catcher of inclined walls including a sacrificial concrete layer, a cooling gap and downcomers and on two trains of gravity-driven cooling water supply systems (Granovsky, 2012).
5.4. Severe accident management guidance (SAMG)

5.4.1. Introduction

Severe Accident Management Guidance (SAMG) provides an organisational and procedural framework for coping with beyond design basis accidents. The principal objective of severe accident management (SAM) is to bring a nuclear power plant to a stable and controlled state following a severe accident (otherwise referred to as beyond design basis accident involving significant core damage with the potential for breach of RPV and containment) and to minimise on-site and off-site radiological consequences. The SAMG framework includes necessary hardware, procedures to prevent and/or mitigate severe accidents and consequences, operator training to implement such procedures, and an organisational structure to oversee the implementation.

Early on in its evolution, the approach to SAMG was to provide a set of accident management procedures, as an extension to emergency operating procedures (EOPs) that cover a finite set of initiating events. Since the TMI-2 accident, the need to consider a broader range of accident conditions and a more complete set of initiating events was identified. The events at Fukushima Daiichi have reinforced this concept. The technical basis for SAMGs was derived largely from the severe accident research activities that followed the TMI-2 accident. It was found that a generic symptom-based approach to SAMG is more appropriate to deal with more complex situations, provided the symptoms and plant parameters associated with them can be correctly identified. This concept facilitated the development of two generic SAMGs for operating reactors (i.e. reactors of the Generation II type) in the United States: Boiling Water Reactor Owners Group (BWROG) SAMG for BWRs, and Pressurised Water Reactor Owners Group (PWROG) SAMG for PWRs. These guidelines are discussed later in this section in more detail.

Generally, the goal of SAMGs is to identify and implement a limited set of strategies that are capable of mitigating the consequences of a severe accident. The symptom-based strategies are intended to minimise the degree to which severe accidents can challenge the integrity of multiple fission product barriers – fuel or core, RPV, and containment structures – that serve to limit potential off-site radiological releases. The following objectives should be accomplished to preserve these barriers:

- Heat removal from overheated core;
- Control of containment atmospheric conditions (temperature, pressure, flammable gas inventory, etc.); and
The SAMG strategies are formulated to perform a few generic actions to meet these objectives. The actions are:

- Restore cooling to the fuel in the RPV, and provide a means to cool the debris in the containment as well as containment atmosphere and spent fuel pool;
- Provide containment overpressure protection by initiating controlled venting;
- Provide means to reduce flammable gas concentration in primary containment and reactor/auxiliary building; and
- Maximise retention of fission products in the containment.

In the aftermath of the accident at the Fukushima Daiichi nuclear power plant (BWR units with Mark I type containment) in March 2011, the nuclear power industry and the regulatory organisations worldwide felt a sense of urgency to revisit the current SAMG and its technical basis. The Fukushima accident brought to the forefront the potential for extreme external events to extensively degrade and even fail the means by which symptom-based accident management measures can be deployed. Work is currently in progress internationally to assess the full extent of consequences of the Fukushima accident and the significant challenges it posed to accident management measures during and following the accident. Some modifications to current SAMG are anticipated in the future as a result of post-Fukushima forensic activities.

The types of challenges to the integrity of fission product barriers that arise during a severe accident vary as do the SAMG strategies to mitigate these challenges. Moreover, the SAMG strategies vary between plant types (BWR vs. PWR for example). Also, the plant-specific SAMG and the means of implementing it are influenced by the status of the plant and symptoms (for example, the extent of fuel damage and challenge to containment integrity). The variation may not be as significant with regard to fuel matrix and cladding as fission product barriers. However, the variation is increasingly more pronounced for RPV or reactor coolant system and primary containment as other barriers. In what follows, plant-specific SAMGs will be discussed in further detail for BWRS and PWRs with particular focus on those aspects of SAMGs that are related to the phenomenological topic of MCCI.

### 5.4.2. BWR Severe Accident Management

Table 5.4-1 summarises the severe accident challenges in aBWR containment, and also the SAMG strategies for mitigation of challenges.

<table>
<thead>
<tr>
<th>Barrier Type</th>
<th>Challenges</th>
<th>SAM strategies</th>
</tr>
</thead>
<tbody>
<tr>
<td>Containment</td>
<td>Overpressure failure</td>
<td>Control containment atmosphere (temperature, pressure), control non-condensable gas generation, also deploy a controlled containment venting strategy</td>
</tr>
<tr>
<td>Containment bypass/isolation</td>
<td></td>
<td>Control containment atmosphere (temperature, pressure), deploy a controlled venting strategy</td>
</tr>
<tr>
<td>Direct containment heating</td>
<td></td>
<td>Depressurise RPV(^1)</td>
</tr>
</tbody>
</table>

1. If the automatic depressurisation system (ADS) is available during an extended accident, then direct containment heating does not pose a challenge and no additional SAMG strategies are required.
Table 5.4-1: Challenges and SAM strategies for BWR (Cont.)

<table>
<thead>
<tr>
<th>Challenge</th>
<th>SAMG Strategy</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ex-vessel steam explosion^2</td>
<td>Control containment atmosphere and drywell cavity water level management</td>
</tr>
<tr>
<td>Flammable gas combustion in containment and reactor building</td>
<td>Control containment atmosphere and provide hydrogen management systems (igniters, PARs, etc.)</td>
</tr>
<tr>
<td>Liner failure^3</td>
<td>Flood drywell cavity</td>
</tr>
<tr>
<td>Core-concrete interaction</td>
<td>Flood drywell cavity</td>
</tr>
<tr>
<td>Thermal degradation of penetration seal causing leakage^4</td>
<td>Control containment temperature</td>
</tr>
</tbody>
</table>

Of the above challenges, the ones most pertinent to the phenomena of MCCI and debris coolability are: core-concrete interaction, containment over-pressurisation from non-condensable gas build-up, and liner failure. The common SAMG strategy to mitigate these challenges is to provide water in the drywell cavity by means of flooding and/or spray. Another mitigation measure to supplement the provision for water management is containment venting. These particular challenges (for both BWRs and PWRs) and corresponding SAMG actions are discussed below in further detail.

Core–concrete interaction

In a postulated core melt accident, if the molten core is not retained in-vessel despite taking severe accident mitigation actions, the core debris will relocate into the reactor cavity region. The resultant interaction with structural concrete could potentially result in basemat failure (through erosion) and/or containment over-pressurisation (by non-condensable gas build-up) and consequent fission product release to the environment. Although this is a late release event, the potential radiological consequences (in terms of land and groundwater contamination, as well as latent cancer risk), could be substantial and warrant effective strategies to prevent or mitigate such a release. As one of several strategies, the SAMG for many operating LWRs includes flooding the reactor cavity in the event of an ex-vessel core melt release. The effectiveness of cavity flooding to cool ex-vessel core debris and mitigate core-concrete interaction (CCI) depends, among other factors, on the mode and timing of water addition, as well as the heat transfer characteristics of the melt-water interaction.

Flooding the containment at or near vessel breach is considered to be one severe accident mitigation strategy, among others. Flooding provides a water pool in the containment to cool the core debris. In the early phase of the cooling process, core debris in contact with an overlying water undergoes efficient cooling that is dominated by convection, resulting in bulk cooling of the entire melt mass. The melt sparging rate is also high in this initial phase which precludes stable crust formation at the melt-water interface. However, the sparging rate gradually decreases due to efficient heat transfer, and a crust eventually forms. Absent any other cooling mechanisms, the bulk cooling mechanism is limited by a maximum thickness of solidified melt below the melt-water interface. The underlying core debris will continue to interact with the surrounding concrete unless another cooling mechanism is in place.

2. The ex-vessel steam explosion issue is still not fully resolved from a risk perspective and there residual uncertainties remain regarding the explosion loads and containment structural integrity.
3. Liner failure issue is particular to the Mark I containment type and the issue is considered resolved from a risk perspective (see NUREG/CR-5423 and NUREG/CR-6025) provided drywell is flooded at or prior to vessel failure and adequate provision is made for liner submergence.
4. One of several possibilities explored in the context of Fukushima Dai-ichi accident. Other possibilities are drywell head flange leakage, gasket failure, etc. – all potentially caused by excessive temperature.
After stable crust formation occurs, cracks and fissures develop in the solidifying material. These cracks and fissures serve as pathways for water ingression into molten core material, and provide augmentation to what would otherwise be an inefficient conduction-limited cooling process. The effectiveness of this mechanism decreases with increasing concrete content in the corium and has been found to not be highly sensitive to concrete type.

Another mechanism of cooling the core debris is by eruption of the molten material underneath the crust formed at the melt water interface. The volcanic-type eruption process is brought about by gases generated as a result of core-concrete interaction. The eruption mechanism is particularly effective at high gas flow rates and at an initial stage of melt solidification when crust formed at the melt water interface is not sufficiently thick or stable. This eruption mechanism results in transport of melt through the breached crust into the overlying water pool where the melt is quenched to form a coolable particle bed.

A third cooling mechanism that can supplement the otherwise conduction-limited cooling is macroscopic crust breach. As noted previously, cracks and fissures in the crust provide pathways for water ingression into molten material underneath the solidified core debris. Formation of substantially wider and possibly interconnected cracks (macroscopic crack network) in a long-span crust can further enhance water ingress cooling. Also, a long-span crust (typical of reactor scale) can collapse from hydrostatic loading due to standing water atop the crust. This type of crust failure (akin to macroscopic crust breach) can result in substantial water ingress into molten core debris thus providing additional cooling.

Containment over-pressurisation

The discharge of steam and also non-condensable gases such as hydrogen, carbon monoxide, and carbon dioxide from core-concrete interaction (that includes metal-water reactions) will likely increase the internal pressure of containment. Flooding the drywell cavity will have a beneficial effect of cooling the debris and slowing down core-concrete interaction and hence, slowing down non-condensable gas build-up. Moreover, flooding and spray actions will have a beneficial effect on fission product scrubbing. However, if the water is present in the cavity prior to vessel breach, there is some risk of an ex-vessel steam explosion depending, among other things, on the depth of water pool. An appropriate SAMG action is predicated on all these considerations.

Despite water management in the cavity, if the static overpressure in the containment keeps increasing and eventually exceeds the containment pressure limit, failure will occur. The failure could be that of containment penetration(s), containment seal(s) (such as the drywell head flange in BWR Mark I containment designs), or a gross failure of the containment structure. Over-pressurisation could be reduced by controlled venting and condensation of steam by containment cooling. The role of overpressure in secondary containments is different because these are not designed as leak-tight structures. Rather, these structures are designed in some instances with blowout panels to relieve overpressure from events such as main steam line breaks (for example, at the Fukushima Daiichi units).
Liner failure (BWR Mark I)

In BWR Mark I containments, relocation of molten core debris into the drywell cavity pedestal region occurs upon RPV failure. The liner failure issue arises because the tight containment floor geometry relative to the large core inventory can lead to molten core debris flowing a short distance and contacting the drywell liner. In the absence of water on the drywell floor, the thermal load imparted on the liner by the debris will breach the liner leading to containment failure. This containment failure mode, specific to Mark I containments, can be mitigated by providing water on the drywell. Past studies (Theofanous, Amarasooriya, Yan, & Ratnam, 1991) have shown that flooding the drywell with water significantly reduces the thermal load on the drywell liner to the point at which liner failure is highly unlikely (i.e. conditional probability less than 10⁻³ given a core melt accident).

5.4.3 PWR Severe accident management

Table 5.4-1 summarises severe accident challenges in a PWR containment, and also the SAMG strategies for mitigation of those challenges.

<table>
<thead>
<tr>
<th>Barrier Type</th>
<th>Challenges</th>
<th>SAM strategies</th>
</tr>
</thead>
<tbody>
<tr>
<td>Containment</td>
<td>Overpressure failure</td>
<td>Control containment atmosphere (temperature, pressure), control non-condensable gas generation, also deploy a controlled containment venting strategy</td>
</tr>
<tr>
<td></td>
<td>Containment bypass/isolation</td>
<td>Control containment atmosphere (temperature, pressure), deploy a controlled venting strategy</td>
</tr>
<tr>
<td></td>
<td>Direct containment heating</td>
<td>Depressurise RPV</td>
</tr>
<tr>
<td>Barrier Type</td>
<td>Challenges</td>
<td>SAM strategies</td>
</tr>
<tr>
<td>Containment</td>
<td>Ex-vessel steam explosion</td>
<td>Control containment atmosphere and reactor pit cavity water level management</td>
</tr>
<tr>
<td></td>
<td>Flammable gas combustion in containment and reactor building</td>
<td>Control containment atmosphere and provide hydrogen management systems (igniters, PARs, etc.)</td>
</tr>
<tr>
<td></td>
<td>Core-concrete interaction</td>
<td>Flood reactor pit cavity</td>
</tr>
<tr>
<td></td>
<td>Thermal degradation of penetration seal causing leakage</td>
<td>Control containment temperature</td>
</tr>
</tbody>
</table>

Of the above challenges, the one most pertinent to the phenomena of MCCI and debris coolability is containment over-pressurisation from non-condensable gas build-up. The common SAMG strategy to mitigate this challenge is to provide water in the cavity by means of flooding and/or spray. Another mitigation measure to supplement the provision of water management is containment venting.

The Operating Strategies for Severe Accidents (OSSA) framework (Sauvage, Prior, Coffey, & Mazurkiewicz, 2006) was developed for Gen-3+ plants (like the EPR™) which have dedicated, generally passive severe accident mitigation measures implemented into their design. These measures are supported by instrumentation which is adequately qualified and can be credited under severe accident conditions. The OSSA framework is intended to support the emergency team in managing the severe accident by recommending various actions deduced from deviations between the course of events and the intended mitigation path. OSSAs replace the former emergency operating procedures (EOP), while the SAMGs typically extend them.
5.5. **Plant applications of MCCI**

5.5.1. **MCCI under dry cavity conditions**

Station blackout scenarios can potentially cause the reactor core injection cooling (RCIC) system for BWRs or auxiliary feedwater (AFW) system for PWRs to be inoperable thus leading to core melting, reactor vessel failure, and subsequent relocation of core debris onto a dry cavity floor (if the SAMG for a particular plant or plant type has no provision for a pre-flooded cavity) and consequent dry MCCI, i.e. MCCI under dry cavity conditions. The cavity may not be fully dry, strictly speaking, in many, if not most, plants. This is because there is always some nominal leakage during normal operation (e.g. seal leakage, penetration, etc.). However, this small amount of water will quickly boil off upon relocation of core debris on the cavity floor.

During the MCCI, depending on the progression of the melt and the particulars of the containment design, the corium mixture may relocate into other rooms when walls are penetrated (e.g. see Section 2.5 summary of the Chernobyl MCCI progression). The ability of melt to spread into an adjacent room once a wall is breached (provided that these rooms are also initially dry) is determined by the temperature, composition (through the melt viscosity), breach flow area, and also the gravity head.

In terms of safety criteria, the purpose of MCCI simulations is to establish the time corresponding to the loss of containment integrity that prevents fission product leakage through the failure location. Given a realistic containment design, it is important to focus the analysis on the weakest point in that design, which could be sumps, instrumentation pathways, and/or collecting channels or pipes embedded in the basemat in the reactor pit. Also, melt relocation into an adjacent room could provide bypass or constitute the weakest point in the design.

In some reactor designs, early radial melt through could lead to more rapid containment failure than axial melt through under circumstances that include:

- Bypass to a lower room or spreading in a sump or other structure where the axial concrete thickness is smaller, or
- When a liner is present and failure of this liner is regulatory considered as a containment failure, lateral spreading to a position where the liner could be contacted.

Given the details of the containment design, both MCCI and spreading simulation tools may be needed to evaluate with confidence the potential for containment failure by core-concrete interaction. Uncertainties regarding the 2D progression of the melt during MCCI as well as the potential for the material to spread needs to be addressed using a bounding approach that factors in weak points in the containment design.

5.5.2. **MCCI under flooded cavity conditions**

Depending on the details of the accident sequence, the availability of safety systems and operator actions, and the cavity geometry, water may be present on the cavity floor prior to vessel failure. Pre-existing water has several potential consequences regarding MCCI behaviour:

- Corium-water interaction could lead to the formation of a particle bed that may or may not be coolable (independently of the steam explosion risk).
- The corium mixture may be partially or fully solidified, depending on the pour rate and masses of material involved (both water and corium), with the possibility to form a mound or a pile.
- If a debris bed locally re-melts and forms a corium pool, subsequent spreading under water onto the surface of the cavity is expected to be dramatically slower in comparison to a dry
cavity situation (similar to the lava flows under water, with successive crust formation / crust failure events leading to step-wise melt relocation behaviour).

Depending upon the extent of the debris porosity, the time for the corium debris to re-melt can be estimated with varying degrees of accuracy. There are ongoing analytical and experimental investigations in this area, primarily focused on multi-dimensional aspects of debris bed dry-out behaviour that should reduce uncertainties in these types of evaluations in the future.

5.5.3. Theoretical basis for plant calculations

There are various levels of sophistication currently employed in plant analyses regarding MCCI behaviour. The simplest approach is to assume that, following vessel failure, the corium spreads instantaneously over the entire available area in the reactor pit. This scenario can be described by virtually all available MCCI codes today. However, more realistic scenarios involve additional factors that can include:

- Limited corium spreading in the reactor pit.
- The presence of water with possible sustained water injection.
- Transfer to adjacent compartment(s) with varying elevation(s), leading to different melt depths and different interaction geometries (e.g. typical cylindrical, notch, rectilinear).

These phenomena, possibly combined and/or coupled, could change considerably the subsequent evolution of the ablation pattern, with potential variations in melt-through times in different compartments. The following realistic factors may be expected:

In the case of corium relocation into a localised area within a large containment, a limited fraction of the available floor area may be involved due to limitations on spreading resulting from a high corium solid fraction, or complete solidification during spreading (see Figure 5.5-1). The extent of spreading will influence the ablation kinetics because of the initially reduced contact area between the corium inventory and the reactor pit concrete basemat and walls. There is a potential for subsequent evolution of the debris bed (i.e. re-melting of solid debris, spreading, and delayed onset of ablation).

With pre-existing water on the containment floor, the water may quench or partially solidify the relocating corium, thus requiring the corium to reheat before concrete ablation can be established. The presence of water may also hinder corium spreading, particularly if the corium solid fraction is high, further reducing the extent of core-concrete interaction (due to augmented cooling) compared to a dry case (see Figure 5.5-1). However, if the debris is not fully or partially cooled, then this scenario may lead to locally higher heat fluxes and faster ablation for some length of time until re-spreading occurs.

In the case of a corium transfer to adjacent compartments by failure of walls, localised flow restrictions, or gates that are designed to fail, crucial phenomena to be investigated are the possible plugging within the hole, as well as limitations on the spreading rate due to the flow restriction (see Figure 5.5-2).
Most of the available codes (see Section 4) can describe the impact of water injection and top quenching, but within the confines of the originally assumed reactor pit geometry. There are limited capabilities for evaluating the restriction of concrete ablation to a part of reactor pit walls due to localised corium accumulations (as in Figure 5.5-1); see Section 5.5.4.

There are limitations with the current set of codes in their ability to model additional spreading of corium outside the initial cavity as shown in Figure 5.5-2 cannot be modelled at the current time. Some codes are able to model spreading, but most only cover the initial spreading phase(s) from the reactor vessel involving hydrodynamics and corium freezing processes. Subsequent corium re-melting and refreezing, as well as spreading involving wall failure(s), is currently not treated.

This summary shows that additional model development to treat more realistic scenarios is needed to reduce residual uncertainties and to gain more safety margin. In particular, no dedicated models are available to treat scenarios where the core debris may re-melt, leading to re-spreading of core debris, as well as situations where core debris can relocate through pathways after localised failures by ablation. There has been progress made on the ability to treat scenarios involving corium spreading into more complicated containment geometries, with localised ablation treated within these structures, an example of which is provided in the next section.
5.5.4. Results and outcomes of some benchmarking activities made on generic plants

5.5.4.1. Benchmark on a generic LWR situation

The MCCI reactor benchmark performed in the frame of the European SARNET Network of Excellence in 2008 (Cranga, et al., 2010) compared predictions for a generic light-water reactor case obtained by most of the available MCCI codes for a basemat thickness of up to 6 m. The objective of this benchmark was to compare the trends of different code predictions in terms of a homogeneous pool configuration with an isotropic heat flux distribution, as well as a configuration permitting pool stratification.

The inventory was typical of that of a large BWR which explains the fast ablation kinetics even in the case of a homogeneous pool assuming an isotropic heat flux. Main results are displayed in Figure 5.5-3 and Figure 5.5-4.

![Figure 5.5-3: Ablation kinetics assuming a homogeneous pool, LCS (left) and siliceous (right) concretes see (Cranga, et al., 2010)](image1)

![Figure 5.5-4: Ablation kinetics with possible pool stratification: LCS (left) and siliceous (right) concretes from (Cranga, et al., 2010)](image2)

The comparison of code results showed some areas of agreement; i.e. for the homogeneous configuration case, results from all codes are near each other for limestone-common sand concrete and, to a less extent, for siliceous concrete (differences in the axial melt-through for a basemat depth of 4 m at most by a factor of 2), except for MELCOR (UPM) and CORQUENCH (VTT) codes. But they also exhibited strong discrepancies:
• For the case of a homogeneous melt configuration with siliceous concrete, MELCOR and CORQUENCH codes deviate substantially from other codes. Besides reasons due to model differences and different thermo-chemistry data, the impact of the particular cavity erosion algorithm in the MELCOR code and of very simplified assumptions for determining the cavity shape in the CORQUENCH code contribute to the deviations of these two codes compared to the others.

\[\text{Figure 5.5-5: Cavity shapes with possible stratified pool for LCS after 4 days of interaction (left) and siliceous (right) concretes from (Cranga, et al., 2010)}\]

• For the case in which the pool can stratify, deviations between codes become very large mainly due to differences in configuration evolution assumptions and the oxide/metal heat transfer model. Code deviations are still greater in case of siliceous concrete compared to limestone-sand concrete because the stratified configuration lasts a longer time.

These results pointed out the need to improve the reliability of the pool configuration model and the heat transfer model at the metal/oxide interface in case of a stratified pool particularly for typical BWR core inventory configurations. They also illustrate the substantial need to validate the codes against available 2D MCCI tests. It is also important to perform additional reactor material experiments using siliceous concrete to further validate MCCI models in the case of siliceous concrete, where the code discrepancies are larger and melt-through of the basemat might occur earlier than in the case of limestone-sand concrete.

5.5.4.2. Benchmark on a VVER 1 000 situation

As an example some results are given for a MCCI reactor benchmark performed in the early 2010s in the frame of SARNET and organised by INRNE on a VVER1000 reactor type (Gencheva, et al., 2012). This benchmark confirmed general trends obtained by previous studies, in particular the MCCI reactor benchmark exercise of previous Section, on the influence of stratification criteria and of the level of the oxide/metal convective heat transfer.

The results of this study indicate that for VVER1000 reactors with a high iron mass fraction concrete basemat (around 16% instead of around 7% in PWR), the stability of the stratified configuration with metal layer below might be enhanced (see Figure 5.5-7), resulting in a melt-through delay in the range of 24 to 30 h after MCCI onset (see Figure 5.5-8) when using the same pool stratification assumptions and models as in PWR applications.
Seven calculations without quenching have been done with different MCCI computer codes: six with MEDICIS/ASTECv2 code, and one with the WECHSL code. Calculations of the cavity boundary profile evolution with MEDICIS/ASTECv2 under dry conditions are presented in Figure 5.5-6 (INRNE calculation). Starting from an initially stratified pool configuration with the metal above the oxide, a pronounced radial ablation is first observed in the INRNE computational results. After approximately 3 hours, the configuration then switches to homogeneous. A secondary stratification with metal below appears at 12.6h after MCCI onset which results in faster axial erosion until basemat melt-through is reached at 27h. Similar results are obtained by most of the other organisations using ASTEC-V2 (IRSN, NUBIKI, TU). In these calculations, the standard Greene’s correlation for evaluating the oxide/metal heat transfer was used. Accelerated axial ablation after pool stratification with metal below occurs; this is due to the impact of high oxide/metal convective heat transfer coefficient and the focusing of decay energy downwards in the final stratified phase. In the GRS ASTEC-V2 calculation, the pool is assumed to be stratified all the time with the metal layer below the oxide but with a lower heat transfer coefficient between the oxide and metal. In the KIT (WECHSL) calculation the metal phase is assumed to remain homogeneously dispersed in the form of droplets. The evolution of the metal layer mass is displayed below in Figure 5.5-7. The thickness of this layer that is set to zero in a homogeneous configuration is reduced in stratified conditions due to oxidation that mainly occurs by oxidation from concrete decomposition gases (steam and carbon dioxide). These different assumptions lead to significant differences in the axial melt through delays displayed in Figure 5.5-8.

Figure 5.5-6: Evolution of cavity boundary profile versus time in INRNE calculation (MEDICIS/ASTECv2) in dry conditions from (Gencheva, et al., 2012)

Figure 5.5-7: Mass of the metal layer in dry conditions from (Gencheva, et al., 2012)
Due to the high iron content in concrete and the rather low gas content, which both contribute to maintain the pool stratification with metal below until the end of the calculation, comparable fast axial ablation and early melt-through have been observed for both dry MCCI conditions and in the case where water is injected on top by INRNE and IRSN that computed both situations using the simplified coolability models available at that time in MEDICIS\ASTEc\2, see Table 5.5-1. The limited impact of top flooding on melt-through delay in this typical VVER configuration (around 16% iron mass fraction compared to around 7% for a PWR) would have to be re-assessed in case of significant advances in knowledge related to the effect of metal on both ablation and cooling derived from the experiments and studies recommended later in this chapter.

5.5.4.3. Support to the assessment of EPR\textsuperscript{TM}

To be able to demonstrate that, for all considered severe accident scenarios, the core debris will be completely released from the RPV before the end of the molten core interaction with sacrificial concrete in the pit and thus before the start of melt spreading, the MCCI code COSACO, see Chap. 3.2.6, was complemented by a simple physical model for the lower RPV. In this model, the RPV lower steel structures (incl. core support plate and lower head) are lumped into a single mass denoted as “RPV-bottom”. Starting with the first release of melt into the pit, the model calculates the heat–up of this structure considering solely the radiant heat emitted from the surface of the MCCI pool (see Figure 5.5-9).
In the analyses the RPV-bottom is assumed to ultimately fail once its temperature exceeds a pre-set value (1300°C). At this instant, the steel mass associated with the RPV-bottom and all remaining in-vessel core debris is added to the MCCI pool.

**Selected modelling assumptions**

The cylindrical geometry of the EPR™ pit is modelled according to the scheme given in Section 3.2-5. Only the sacrificial concrete layer is taken into account. Its outer perimeter is defined by the protective layer, which is considered to form a static, adiabatic boundary. This is justified by the very low thermal conductivity of the protective material and its experimentally confirmed stability against both the metallic and oxidic melt fraction under MCCI conditions (Hellmann & Fischer, 2007).

To account for the uncertainties related to the gas-induced mixing between the oxidic and metallic phase of the melt, two enveloping cases are considered for the analysis: the layered case (L), in which all constituents of the MCCI pool, namely metal, oxide and slag (concrete accumulating atop the upper metal layer) are stratified according to their densities and the mixed case (M), in which all constituents are mixed and in thermodynamic and chemical equilibrium. In the layered case, the core oxide phase always has a higher initial density than the steel. This is because any in-vessel “dense metal” phase does not remain under the oxidising conditions during MCCI. The subsequent addition of concrete decomposition products steadily reduces the density of the oxidic melt $\rho_{\text{oxide}}$ up to the point when it becomes less than the density of the metal phase $\rho_{\text{metal}}$. Therefore, the layered case (L) includes the following two situations:

\[
\rho_{\text{oxide}} > \rho_{\text{metal}} \gg \rho_{\text{slag}} \quad \text{(before layer inversion),}
\]

\[
\rho_{\text{metal}} > \rho_{\text{oxide}+\text{slag}} \quad \text{(after layer inversion)}.
\]

During the layer inversion the core oxides take the top position. The related incorporation of the accumulated concrete slag causes a drastic drop in oxide density.

For the heat transfer to the concrete, the COSACO model predicts a nearly isotropic heat flux distribution inside the oxidic/mixed pool due to effective forced convection heat transfer (gas induced mixing), consistent with the results of the BALI experiments (Bonnet, 1999). These tests only model the pool of the MCCI but not the specifics of the concrete erosion process. Therefore, they cannot explain the faster radial melt progression observed in some 2D VULCANO and CCI experiments with (non-reinforced) siliceous concrete, including EPR™ sacrificial concrete (see Chapter 2). With respect to the proof of the retention function the isotropic model is nevertheless conservative as it maximises axial erosion and thus leads to earlier gate melt-through.
Melt release from the RPV

Predicting the melt masses in the lower head at the time of RPV melt-through as well as the fraction of this mass which is initially released into the pit entails significant uncertainties. The approach chosen to deal with these uncertainties is to perform a bounding parametric variation of the masses of oxidic and metallic melt released during the first pour which envelops the range of predictions of integral codes.

In combination with: (i) conservative assumptions for the progression of the melt towards the gate and (ii) a large variation of release time / decay power, this procedure effectively covers a wide spectrum of severe accident scenarios, including Large(LB) / Small (SB) Break LOCAs.

Example of results for the MCCI in the reactor pit

The evolution of cavity profiles for an example LBLOCA calculated with either the layered (L) or mixed (M) modelling option, is shown in Figure 5.5-10 for an assumed single-pour melt release from the RPV. With the layered (L) assumption (left figure), the metal layer quickly expands in radial direction. During this early phase, all zirconium contained in either the metallic or oxidic phase is oxidised. The early contact with the protective layer insulates the metal layer along its circumference and thus keeps its temperature high. Consequently, the heat fluxes and ablation rates in the metal region after the ultimate layer flip are high, as they are supplied by the transient cool-down of the metal layer.

![Figure 5.5-10: Erosion profiles for a typical LB-LOCA scenario with assumed layered (L on left) and mixed (M – right) melt configurations](image)

When applying the mixed melt assumption, the oxide phase is initially subcooled, because of the postulated global thermal equilibrium. The calculated melt progression is therefore more complex as it is influenced by the interplay between oxide phase reheating, gas mixing and crusts melting. Depending on oxide superheat, the MCCI can temporarily involve high rates of erosion and gas generation, as well as correspondingly high void and pool levels.
When multiple pours are assumed the transient evolution of erosion profiles becomes more complex. However, to compare the various transient cases it is sufficient to compare their axial ablation curves. Such example curves are given in Figure 5.5-11. The convention used to denote these cases is: the first number (e.g. 40%) refers to the initial corium release fraction, while the two letters denote the scenario: early (E, straight lines) or late (L, dashed lines) and the melt state: layered (L) and mixed (M), respectively.

Figure 5.5-11 illustrates that ablation progresses slower for: (i) late scenarios (less decay power), (ii) lower initial melt release fractions, and (iii) mixed melts, when compared to their layered counterparts. It demonstrates that – for all analysed scenarios and assumptions – the “Failure of RPV bottom” event occurs significantly before the melt has penetrated even half of the sacrificial concrete layer provided above the gate. This proves the self-adjusting characteristic of the temporary melt retention function. It seems worth emphasising that this function is not based on guaranteeing a certain minimum duration of the retention period (it can vary with decay power and melt release sequence), but a maximum erosion depth at the time when all core debris is present in the MCCI pool!

Once this is achieved the MCCI continues and further concrete decomposition products are added to the oxidic melt, which causes a steady cool-down in accordance with the related decrease in melt liquidus temperature.

The next important issue is the assessment of the spectrum of melt states at the end of the pit MCCI, as this is needed to demonstrate the ability of the melt to spread. Figure 5.5-12 gives the corresponding predictions for the final melt composition showing that for all analysed parametric cases, the melt composition becomes dominated by concrete decomposition products and iron and chromium oxide originating from the oxidation of steel (in total >50mol%).

The only small differences in the final melt composition and in particular in the core oxide content between the analysed cases translate into correspondingly small variations in melt temperature and material properties. In addition, also the final masses of the melt stay within a narrow range. This is due to the presence of the protective layer which prescribes the amount of (sacrificial) concrete to be incorporated into the melt.

This remains true also if radial ablation would be considered to be more pronounced than the axial one. This is because the melt now reaches the cylindrical part of the protective layer earlier, leading to a transition from 2D MCCI into a 1D MCCI. As a consequence, melt temperatures will increase and a comparably higher fraction of the decay power will be transported to and emitted from the upper free surface. This in turn will result in an earlier failure of the RPV bottom, a later failure of

**Figure 5.5-11:** Ablation front progression and RPV-bottom failure for various outflow sequences
the gate and – because of the higher temperature – a lower viscosity of the melt. All these changes have a positive impact on melt accumulation and conditioning.

Figure 5.5-12: Calculated Oxide melt compositions (in mol%) at the end of the temporary retention in the pit

The results of the presented analyses attempt to demonstrate that the retention phase in the pit decouples the later processes in the core catcher from the preceding events, including the accident scenario, in-vessel melt progression, RPV failure mode, and melt release sequence.

5.5.4.4. Plant application made in the US

The severe accident source terms for selected accident sequences in a BWR Mark I plant were analysed using MELCOR in a 1993 study (NRC, 1993) for the purpose of comparing the MELCOR results with the NUREG-1150 (NRC, 1990) results calculated by the Source Term Code package (STCP). The three severe accident sequences are: low pressure shut down, short term SBO, and design basis LOCA, concurrent with complete failure of the emergency core cooling system. For the short term SBO scenario, two cases were run: one with dry cavity and one with flooded cavity. A second study (BNL, 1990) involved MELCOR calculations of the long term SBO in a BWR Mark I plant with failure to depressurise the reactor vessel, and a comparison with the results calculated by the STCP package. Table 5.5-2 lists a number of plant applications of MELCOR in the 90s.

Table 5.5-2: MELCOR plant applications

<table>
<thead>
<tr>
<th>Plant</th>
<th>Country</th>
<th>Plant and containment type</th>
</tr>
</thead>
<tbody>
<tr>
<td>TMI-2</td>
<td>United States</td>
<td>B&amp;W PWR</td>
</tr>
<tr>
<td>Surry</td>
<td>United States</td>
<td>3-loop Westinghouse PWR</td>
</tr>
<tr>
<td>LaSalle</td>
<td>United States</td>
<td>BWR/5, Mark II</td>
</tr>
<tr>
<td>Peach Bottom</td>
<td>United States</td>
<td>BWR/4, Mark I</td>
</tr>
<tr>
<td>Grand Gulf</td>
<td>United States</td>
<td>BWR/6, Mark III</td>
</tr>
<tr>
<td>Oconee</td>
<td>United States</td>
<td>B&amp;W PWR</td>
</tr>
<tr>
<td>Calvert Cliffs</td>
<td>United States</td>
<td>CE 3-loop PWR</td>
</tr>
<tr>
<td>Zion</td>
<td>United States</td>
<td>4-loop PWR</td>
</tr>
<tr>
<td>Point Beach</td>
<td>United States</td>
<td>2-loop PWR</td>
</tr>
<tr>
<td>Browns Ferry</td>
<td>United States</td>
<td>BWR/4, Mark I</td>
</tr>
<tr>
<td>TVO</td>
<td>Finland</td>
<td>ABB BWR</td>
</tr>
<tr>
<td>Lovisa</td>
<td>Finland</td>
<td>VVER-440</td>
</tr>
<tr>
<td>Muhleberg</td>
<td>Switzerland</td>
<td>BWR/4, Mark I</td>
</tr>
<tr>
<td>Beznau</td>
<td>Switzerland</td>
<td>2-loop PWR</td>
</tr>
<tr>
<td>Gosgen</td>
<td>Switzerland</td>
<td>3-loop PWR</td>
</tr>
<tr>
<td>Leibstadt</td>
<td>Switzerland</td>
<td>BWR/6, Mark III</td>
</tr>
</tbody>
</table>
Many of these plant applications involved core-concrete interaction calculations using the CORCON module in the CAV package of MELCOR. The Peach Bottom and Zion calculations were performed as part of MELCOR/MAAP comparison, whereas Oconee calculations were performed as part of MELCOR/STCP comparison. MELCOR calculations for the Surry plant were performed for updating the source term for three accident sequences (AG, S2D and S3D). A TMLB station blackout analysis was also performed for Surry to compare two different updates of MELCOR. The Browns Ferry (same plant and containment type as Peach Bottom) calculations were done to investigate the relative effect of plant-specific features on accident progression, in particular, ex-vessel phenomena. MELCOR calculations for the LaSalle plant were performed as part of an integrated risk assessment in the phenomenology and risk uncertainty evaluation programme. The Grand Gulf calculations were performed as part of the Level 3 PRA to assess severe accident risks under low power and shutdown conditions.

Some of these plant calculations have been repeated in later years with updated code versions of MELCOR for new regulatory applications. MELCOR calculations were also performed in the late 1990s and early 2000s as part of the confirmatory safety analysis of advanced reactor designs (generation III varieties). Notable among these applications are those relating to the ABWR, ESBWR, AP600/AP1000, and EPR plant designs.

The ABWR MELCOR calculations were performed for two low-pressure and three high-pressure sequences to study the potential effects of core-concrete interactions, and to evaluate the source terms including those generated by such interactions (Kmetyk, 1994). Sensitivity studies were done on the impact of assuming limestone rather than basaltic concrete and on the effect of quenching core debris in the cavity compared to having hot, unquenched debris present.

The accident scenario for ESBWR calculations involved a transient initiated by a loss of power. GDCS injection to the reactor pressure vessel (RPV) and wetwell injection to the RPV through equalisation lines are not available. The ADS system is actuated if and when the water level in the RPV downcomer reaches a specified level. The PCCS and the pool makeup system are available, thus allowing long-term containment heat removal. However, the GDCS deluge and the basemat internal melt arrest and coolability (BiMAC) system are not available for debris bed cooling. In addition, containment venting is not available. Because the GDCS deluge and the BiMAC system are not credited in this scenario, MCCI is predicted to occur following relocation of core debris from the breached reactor vessel. MCCI results in concrete basemat erosion and production of concrete decomposition gases.

MCCI calculations were performed for the scenario for accident duration of 48 hours using MELCOR code version 1.8.6. This version, when developed, effectively had no phenomenological (i.e. physics-based and supported by experimental data) model for ex-vessel debris coolability with an overlying water pool. Heat flux partitioning (i.e. heat flux from melt to overlying water pool and heat flux from melt to basemat as well as sidewall) in the melt pool is performed in an empirical manner. User guidance (best practice values) is provided for the partitioning ratios, based on calibration of calculated thermal-hydraulic signatures with selected test data. Using this version, the erosion of the

<table>
<thead>
<tr>
<th>Plant</th>
<th>Country</th>
<th>Plant and containment type</th>
</tr>
</thead>
<tbody>
<tr>
<td>&quot;José Cabrera&quot;</td>
<td>Spain</td>
<td>1-loop PWR</td>
</tr>
<tr>
<td>Garona</td>
<td>Spain</td>
<td>BWR/3, Mark I</td>
</tr>
<tr>
<td>Almaraz (I and II)</td>
<td>Spain</td>
<td>3-loop PWR</td>
</tr>
<tr>
<td>Cofrentes</td>
<td>Spain</td>
<td>BWR/6, Mark III</td>
</tr>
<tr>
<td>Asco (I and II)</td>
<td>Spain</td>
<td>3-loop PWR</td>
</tr>
<tr>
<td>Trillo</td>
<td>Spain</td>
<td>3-loop KWU</td>
</tr>
<tr>
<td>Vandellos</td>
<td>Spain</td>
<td>3-loop PWR</td>
</tr>
</tbody>
</table>
basemat concrete at the end of the calculation was calculated to be about 1.5 m in the downward direction and 1.7 m in the radial direction (Figure 5.5-13).

![Figure 5.5-13: Calculated axial and radial ablation for an ESBWR transient](image)

As a result of continued MCCI as well as continued ex-vessel oxidation of remaining core zirconium, large quantities of hydrogen and carbon monoxide were generated (Figure 5.5-14). The amount of carbon dioxide production was negligibly small because of the basaltic concrete composition assumed for the drywell floor. MCCI calculations were also performed for a variation of the accident scenario whereby the GDCS deluge is successful and thus credited.

![Figure 5.5-14: Non-condensable and steam production from core-concrete interactions for an ESBWR transient](image)

The ablation profile for this case is plotted in Figure 5.5-15. The non-condensable gas generation is plotted in Figure 5.5-16.

![Figure 5.5-15: Calculated axial and radial ablation for an ESBWR transient where GDCS deluge is successful](image)
Calculations were also performed for the AP1000 design using the MELCOR Code Version 1.8.6 to confirm the amount of basemat ablation. The calculations showed a maximum ablation depth of about 1.3 m for both limestone and basaltic concrete 2.5 days after accident initiation, assuming a dry reactor cavity and uniform distribution of debris within the reactor cavity. The dry reactor cavity assumption was merely a simplification reflecting a bounding scenario. The ablation rates predicted by MELCOR were lower than those predicted by MAAP, partially as a result of late RPV failure in the MELCOR calculation (8 hours in MELCOR versus 2 hours in MAAP).

The accident scenario for AP1000 calculations involved a nominal 17 cm break in the direct vessel injection (DVI) line located in one of the two reactor coolant system loops, resulting in flow of water from one accumulator, one core makeup tank and the in-containment refuelling water storage tank into the lower containment region, including the cavity. The ADS is successfully actuated, resulting in a low reactor coolant system pressure during core damage. Even though one of the accumulators and a CMT are assumed to inject water into the RPV to replenish the RCS inventory, these systems are not sufficient to make up for the loss of inventory through the DVI line break. The PRHR system is assumed to be unavailable for decay heat removal during this accident scenario.

Another accident scenario involves a spurious actuation of the ADS stages 1, 2 and 3 resulting in the loss of RCS inventory eventually leading to a “medium” pressure inside the reactor coolant system. Following the reduction of the reactor coolant system pressure below the accumulator injection set point, it is assumed that one of the two accumulators would be able to inject. The failure of the CMT injection and stage 4 ADS is also assumed, thereby, preventing automatic IRWST injection for core cooling. Furthermore, the PRHR system is assumed unavailable for decay heat removal during this accident scenario.

Figure 5.5-17 shows the maximum concrete erosion in the radial and the axial (downward) directions. After 60 hours into the accident, the basemat is calculated to erode over 1.0 m in the radial direction and about 1.25 m in the downward (axial) direction. The concrete basemat integrity is not expected to be challenged for many more hours in this case; nevertheless, the containment steel liner is predicted to be penetrated in about 44 hours into the accident. Figure 5.5-18 shows the equivalent erosion plots for the case of basaltic concrete (sensitivity case 2).
MELCOR calculations were performed for the eight sensitivity cases listed in Table 5.5-3 in order to assess the impact of MCCI, the containment spray actuation, and the containment shell exterior water coverage on the containment response behaviour in AP1000. The RPV failure modes, cavity conditions, concrete type, and the actuation of containment sprays are shown in the table. It is assumed in the calculations that any relocated debris will immediately cover the entire cavity floor area.

Table 5.5-3: MCCI sensitivity calculations for AP1000

<table>
<thead>
<tr>
<th>Sensitivity</th>
<th>Debris relocation mode from RPV</th>
<th>Cavity condition</th>
<th>Concrete type</th>
<th>Containment cooling</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Slow</td>
<td>Dry</td>
<td>Limestone</td>
<td>Sprays off</td>
</tr>
<tr>
<td>2</td>
<td>Slow</td>
<td>Dry</td>
<td>Basaltic</td>
<td>Sprays off</td>
</tr>
<tr>
<td>3</td>
<td>Slow</td>
<td>Partially flooded</td>
<td>Limestone</td>
<td>Sprays off</td>
</tr>
<tr>
<td>4</td>
<td>Rapid</td>
<td>Partially flooded</td>
<td>Limestone</td>
<td>Sprays off</td>
</tr>
<tr>
<td>5</td>
<td>Not applicable (IVR)</td>
<td>Deeply flooded</td>
<td>Not applicable</td>
<td>Sprays on for 2 hrs</td>
</tr>
<tr>
<td>6</td>
<td>Slow</td>
<td>Partially flooded</td>
<td>Limestone aggregate/common sand</td>
<td>Sprays off</td>
</tr>
<tr>
<td>7</td>
<td>Not applicable (IVR)</td>
<td>Deeply flooded</td>
<td>Not applicable</td>
<td>Sprays off; partial wetted coverage on containment shell exterior</td>
</tr>
<tr>
<td>8</td>
<td>Not applicable (IVR)</td>
<td>Deeply flooded</td>
<td>Not applicable</td>
<td>Sprays off; 60% wetted coverage on containment shell exterior</td>
</tr>
</tbody>
</table>
The sensitivity case 1 is based on the second accident scenario (i.e. spurious actuation of the ADS stages 1 through 3) resulting in vessel breach. The cavity concrete is limestone aggregate type. It is assumed that the RPV fails on the side resulting in a slow debris relocation rate to the cavity over an extended period of time.

The sensitivity case 4 is a variation of sensitivity case 1 except this case considers a rapid relocation of the core debris from the RPV into a partially flooded cavity. Figure 5.5-19 shows the maximum concrete erosion in the radial and axial (downward) directions. At 60 hours into the accident, the axial erosion is 1.30 m while the radial erosion is less than a meter. Based on the calculated concrete erosion rates in the radial and the axial directions, the penetration of the basemat, in either the axial or the radial direction, is not expected to occur for many days.

![Figure 5.5-19: Calculated axial and radial ablation for an AP1000 transient for a rapid relocation sensitivity case with partially flooded cavity](image)

Two most recent examples of MELCOR plant analysis are: State-of-the-Art Reactor Consequence Analysis (SOARCA), and containment venting analysis. The SOARCA project was a comprehensive study focused on providing a realistic evaluation of accident progression, source term, and off-site consequences for select scenarios for the Peach Bottom plant (BWR Mark I) and the Surry Power Station (PWR) by using the best available MELCOR modelling features. The analysis considered mitigative measures (e.g. emergency operating procedures, SAMG, etc.), plant capabilities, as well as the most current emergency preparedness practices, thus contributing to a more realistic evaluation of consequences. The containment venting study, likewise, is an evaluation of accident progression, source term, and off-site consequences for select scenarios in a BWR Mark I) and the Surry Power Station (PWR) by using the best available MELCOR modelling features.

The MCCI modelling capability of MELCOR is based on CORCON-Mod3. The models assume that a conduction-limited crust forms at the melt-water interface and that this crust is impervious to any form of water ingestion. Thus, MELCOR predicts continued melt-concrete interaction underneath the crust at a higher rate even after cavity flooding and hence, eventual basemat penetration by MCCI. Also, MELCOR calculates the melt spread area through use of a mass balance and a basic empirical hydraulic relationship which spreads the melt based on the melt height and temperature. The code does not currently take into account viscosity or leading-edge freezing effects, and thereby introduces some uncertainties in the melt spread calculation.

Since the CORCON-Mod3 formulation of MCCI into MELCOR, a number of international research programs and experiments have been conducted (notably, MACE, NEA-MCCI1, and NEA-MCCI2). Insights and models from these research activities have been integrated into the stand-alone CORQUENCH code (see Section 4). Phenomena accounted for in CORQUENCH include crust
anchoring and debris cooling by water ingression and melt eruption mechanisms, and these phenomena can have potentially large impacts on MCCI progression. The CORQUENCH code is able to evaluate melt debris coolability under top flooding as well as the axial and radial ablation of concrete due to MCCI in a more realistic manner. Also, with regard to melt spreading physics, a second stand-alone code, MELTSPREAD, was developed. MELTSPREAD calculates the area over which the melt spreads, taking into account a number of physical phenomena including varying melt viscosity, heat transfer, and solidification at the leading edge of the spreading melt. In addition to predicting the lateral spread of the melt, MELTSPREAD has the ability to predict melt-liner attack and failure of the liner if the melt is predicted to contact the drywell liner (near the floor).

Recently, simplified versions of water ingression and melt eruption models akin to those deployed in CORQUENCH have been incorporated into the latest working version of MELCOR (MELCOR 2.1) and the new models are being assessed against experimental data and being benchmarked against other codes.

5.5.4.5. Study of the Fukushima Daiichi Unit 1 (1F1) ex-vessel situation

In a recent analysis of Fukushima Daiichi Unit 1 (1F1) accident, the MELCOR v2.1 code predictions of melt spreading and MCCI were compared to the MELTSPREAD and CORQUENCH 3.03 codes (Robb K. R., 2014). Regarding melt spreading, MELTSPREAD includes relatively detailed modelling of core debris relocation including fluid-mechanics effects, heat transfer to overlying water and underlying concrete, and finally mechanistically calculated solidification at the debris leading edge. In contrast, the MELCOR code predicts the melt spread area through the use of a mass balance and a user-specified hydraulic relationship that spreads the melt based on the melt height and temperature. Despite these differences in modelling approaches, the overall differences in predicted spreading behaviour between the two codes were found to be not that large. Although agreement for this particular 1F1 sequence is reasonable, it is not clear from this study how well the two codes would compare under a different set of conditions such as a hotter or colder melt release. Additional comparison of the MELCOR MCCI module with CORQUENCH under the same set of modelling assumptions (i.e. with the water ingression and melt eruption cooling mechanisms deactivated in CORQUENCH, so that an impervious upper crust boundary condition was applied in both simulations) indicated that the two codes yield similar results in terms of cavity ablation and combustible gas production.

Aside from this comparison, additional analyses were carried out with MELTSPREAD and CORQUENCH to analyse the ex-vessel accident sequence for 1F1 (Robb, Francis, & Farmer, Enhanced Ex-Vessel Analysis for Fukushima Daiichi Unit 1: Melt Spreading and Core-Concrete Interaction Analyses with MELTSPREAD and CORQUENCH, 2013) The high-level objective of this study was to provide best estimate predictions of ex-vessel core melt accident progression and final debris configuration for this reactor accident. The evaluation required the integration of data from several sources. In particular, three sets of melt pour conditions were compiled based on best-estimate MAAP (Luxat & Gabor, 2013) and MELCOR (Gauntt, et al., 2012) simulations. The time-dependent melt pour conditions (composition, flowrate, temperature) were used as input to MELTSPREAD to predict melt propagation, basemat attack, cladding oxidation (viz. H2 and CO production), debris cooling, and drywell liner attack during the transient spreading phase. One of the principal outcomes of this analysis was the extent to which the floor was covered by core debris during spreading, since the depth of debris to be cooled in the long term is inversely proportional to the floor area covered by spreading melt. The MELTSPREAD predictions of the local melt depth, composition, and temperature at the end of the spreading transient were then used to define input for CORQUENCH. CORQUENCH was then utilised to evaluate the long-term debris cooling behaviour in the containment, including the amount of concrete ablation and non-condensable gas generation.

One unique aspect of the CORQUENCH analysis was the development of a cavity discretisation model that allowed localised MCCI behaviour within the pedestal and drywell regions to be analysed.
in an integral manner. To this end, the containment was divided into six regions: sumps (1), inner pedestal (2), inside edge of the pedestal (3), doorway (4) between the drywell and pedestal, an area extending from the doorway to the drywell liner (5), and the far-field drywell (6). These regions are numbered and illustrated in Figure 5.5-20.

![Figure 5.5-20: Containment basemat Discretisation approach for CORQUENCH analysis](image)

The sumps (1) were modelled as 2D cylindrical cavities with walls that are higher than the melt. In reality, the sumps are square and the accumulated melt depth above the height of the sumps will not be in direct contact with the sump walls. The initial collapsed melt depth in the sumps for the MELCOR cases ranged from 0.2-1.9 m while all the MAAP cases were 1.4 m deep. The sumps are 1.2 m deep, and so the majority of the melt was in contact with the sump walls for these cases. The balance of the inner pedestal region (2) included the area between and around the sumps; this region was modelled using the 1D geometry option available as part of the CORQUENCH code input. The pedestal edge (3) region was modelled using a 2D cylindrical geometry similar to that for the sumps. The diameter of the cavity was the same as that of the actual pedestal, 5.0 m. The amount of melt per unit area in the edge region, as determined from the MELTSPREAD results, was extrapolated to fill the inner portion of the 2D cylinder. This conserved the melt height at the pedestal walls and the pedestal curvature. These parameters were conserved in order to conserve the heat and mass transfer at the walls. Other important parameters, such as combustible gas generation during core–concrete interaction, were scaled appropriately. The doorway region (4), the area outside the doorway (5), and the far-field drywell (6) were modelled as 2D notch geometries (also available as a user-defined modelling assumption).

Although this methodology allowed the spatial ablation variation to be evaluated, there were several compromises and limitations associated with the technique. Each region was modelled independently; i.e. heat and mass transfer between regions was not modelled. However, the cross-sectional area of melt in neighbouring regions is much lower than that in contact with concrete and water (or the containment atmosphere), and the lateral heat transfer between zones is expected to be much lower than the heat transfer to the water (or atmosphere) and concrete. The swelling of the melt by the gases released during concrete ablation may cause regions of the melt to rise and spread to other regions. Not allowing the melt to spread from one region to the next results in a less coolable melt configuration. Finally, radial ablation may cause regions to expand into one another. This effect will increase with the extent of radial ablation, but for most regions this influence is expected to be negligible.
The MELCOR and MAAP simulation results for Unit 1 span a wide range of melt pour conditions from the RPV ranging from a gradual pour of low-temperature core debris (MELCOR) to a rapid pour of high-temperature melt (MAAP). The results of the MELTSPREAD analysis were highly dependent upon the pour scenario (see Figure 5.5-21). For the low-temperature gradual MELCOR pour, the code predicted lethargic spreading of a highly viscous melt over a period of approximately 4 000 seconds. Concrete ablation is minimal in these cases since the high viscosity limits convection from the melt to the underlying concrete. Despite the low flow rate, in all cases the melt is predicted to eventually spread out of the pedestal doorway and contact the liner. When water is present, the core debris spreading is limited to a total of approximately 33 m².

In contrast, for the high-temperature MAAP pours, MELTSPREAD predicted the sump plates rapidly ablated through in less than 5 seconds. Furthermore, the concrete erosion during spreading was significant with ~ 2m³ ablated in the first 2 minutes; the maximum ablation depth of ~ 20 cm outside the pedestal door. The melt was also predicted to fully cover the pedestal and drywell annulus floor areas, which sum to approximately 111 m².

Figure 5.5-21: MAAP (left) and MELCOR (right) core debris distributions at the end of the spreading phase

Figure 5.5-22: Cross sections of containment ablation
The results of the long-term debris coolability analysis with CORQUENCH assumed sufficient water was injected into containment to cover the debris starting 15 hours after shutdown. Under this condition the simulations indicate that the melt was coolable over the long term. The predicted concrete ablation was less than that necessary to reach the liner through downward melt progression. The MELCOR case, which contained relatively cool melt, readily cooled within 2.5 hours after relocation with limited concrete ablation in the sump regions (~18 cm) and less than 10 cm ablation elsewhere; see Figure 5.5-22 and Figure 5.5-23. A total of 76 kg of H2 and 103 kg of CO were predicted to be generated during core-concrete interactions. The MAAP cases, which contained relatively hot melt, cooled approximately 22.5 hours after melt relocation and resulted in 65 cm of concrete ablation in the sump region and less than 23 cm elsewhere; see Figure 5.5-24. Large amounts of H2 (700 kg), CO (750 kg), and CO2 (490 kg) were predicted to be generated during concrete ablation for the MAAP cases.

**Figure 5.5-23:** MELCOR cavity profile for cross section A and B after 1 hour of CORQUENCH simulation time and end of simulation
5.5.4.6. Example of PSA2 analysis of MCCI phenomena: German PWR

In 2001 GRS performed a comprehensive probabilistic safety analysis (PSA) with the objective to evaluate the available PSA methods and to demonstrate their usability for practical applications for an advanced German PWR (GRS, 2002). The NPP at Neckarwestheim, unit 2 (GKN-2) was selected as the reference plant for this study. Part of this work consisted of the level 2 PSA for normal power operation, which covered the events from the beginning of core melting up to the release of radionuclides into the environment. For deterministic simulations of the accident progression and thus for safety assessments the integral code MELCOR was used, and for relevant individual aspects concerning the containment atmosphere the code RALOC (Klein-Heßling & Arndt, 1996) was applied.

The principal structure of the event tree used for the PSA is shown in Figure 5.5-25. With regard to MCCI the following branching points are of concern:

- Events between RPV failure and melt-sump water contact, and
- Events after melt-sump water contact.

These events cover the following phenomena related to core melt behaviour in the containment:

- Melt spreading in the lower part of the containment, taking into account possible damage to components (sump suction lines).
• Core-concrete interaction before and after sump water contact, and
• Penetration of concrete foundation.

Figure 5.5-25: Principal set-up of the event tree (GRS, 2002)

The event tree analysis requires that even very complicated processes have to be represented by branching probabilities. Beside the well-known “classical” containment threats related to MCCI – the pressure increase inside the containment due to long-term core-concrete interaction and the potential penetration of the concrete foundation – the reference plant has an additional vulnerability with regard to a possible melt attack on the sump suction lines.

The following sections summarise how these threatening phenomena are considered in the event tree analysis.

Melt spreading in the lower part of the containment

Section 5.3.1.3 identifies a scenario, in which the melt may enter the sump region in the PWR under consideration. There it can spread over an area of approximately 200 m². It is important for the further event progression whether the melt will reach the suction pipe stubs of the emergency core cooling systems.

For the assessment of melt spreading in the sump region the dedicated GRS code LAVA (Allelein, Breest, & Spengler, 1999) was applied. Although the LAVA code is currently not validated for the boundary condition of spreading under water this situation was approximately considered with a reduced force of gravity and an increased heat transfer at the melt surface compared to a dry spreading scenario. Assuming conservative conditions for the initial state of the melt spreading (no initial undercooling of the melt) the LAVA calculations predicted that the melt will cover the whole sump area shortly after it enters the ventilation ducts. Under the conditions described above, the lower part of the sump suction pipes will then be surrounded by core material, but its surface level was estimated to be lower than what would be necessary for a direct flow of core material into the opening of the pipes. It can however not be excluded that the protection tube of these pipes will fail due to the thermal load and melt and/or water may then enter the annulus between protection tube and sump suction pipe. Furthermore, small melt mass flows due to settling of melt particles generated after melt/water contact could also accumulate significant amounts of core material in the inside of the...
sump suction pipes. When the protection tube of the suction pipe develops a leak due to melt impact inside the annulus, there will be a containment leak.

Taking these phenomena into account, the probability of a failure of at least one sump suction pipe was estimated to be between 0% and 10% (homogeneous distribution) for sequences with core melt in the reactor cavity.

Core-concrete interaction before and after sump water contact and penetration of concrete foundation

According to existing experimental evidence a stopping of the core concrete interaction before penetration of the foundation was not implemented in the event tree. This means that gas from the interaction keeps being released. A considerable part consists of non-condensable gases (e.g. hydrogen, carbon monoxide), leading to a continuous pressure increase inside the containment until the pressure relief has to be activated. At the same time this means that all sequences with failure of the RPV lead finally to a penetration of the melt into the ground.

The velocity of the erosion primarily depends on the heat flux from the melt downward into the concrete. This is dependent on the decay heat and thus on the time period since reactor shutdown. Uncertainties are due to the fraction of radionuclides which will leave the melt and the partition of the heat flux to the bottom and to the top.

A number of MCCI calculations were performed with MELCOR version 1.8.4. The calculations considered four variations: 1) three layer pool configuration & gas film model for the heat transfer between melt and concrete; 2) five-layer pool configuration & gas film model; 3) homogeneous configuration & gas film model; and 4) homogeneous configuration and slag layer model for the heat transfer between melt and concrete. Further model options to be selected by the user were selected in agreement with the recommendations by the code developers (NRC, 1997).

Based on these calculations (Figure 5.5-26) and taking into account the results of international research projects on MCCI, for example from the MACE project, probability distributions for axial erosion velocities for different time periods after RPV failure were defined for the event tree analysis.

From that the probabilities for different progress levels of erosion within certain time frames were obtained (Table 5.5-4).
Table 5.5-4: Probabilities for the erosion of the concrete foundation due to core melt

<table>
<thead>
<tr>
<th>Phase of concrete erosion</th>
<th>Probability (first lines) and times (second lines) of the transition from core damage states to various depths of concrete erosion</th>
</tr>
</thead>
<tbody>
<tr>
<td>„Dry period“ after RPV failure before core melt reaches the water filled ventilation ducts</td>
<td>0.06 &lt; 5h 0.32 5-20 h 0.30 20-100 h</td>
</tr>
<tr>
<td>Time between core damage state until melt reaches the containment steel shell embedded in concrete</td>
<td>0.20 &lt; 1 day 0.46 1-5 days 0.02 &gt; 5 days</td>
</tr>
<tr>
<td>Time between core damage state until melt reaches the ground</td>
<td>0.29 &lt; 5 days 0.32 5-15 days 0.07 &gt; 15 days</td>
</tr>
</tbody>
</table>

Table 5.5-4 contains information about times which pass until certain erosion depths (to the ventilation ducts, to the containment steel shell, to the ground) are reached. The values in the second line of the table are typical time periods; the first line (bold numbers) contains the pertinent probabilities (mean over all core damage states). There is a pit at the bottom of the sump where the concrete is thinner than elsewhere (see Figure 5.3-10). This has been taken into account for the time values given.

Conclusions

For all core damage states a small but significant probability of the failure of the containment function due to the melt-through of a sump suction line or due to a failure of the containment pressure relief system was obtained. If the RPV does not remain intact, a core-concrete interaction will take place. A coast-down of this reaction was not assumed. Therefore all sequences with failure of the RPV finally were assumed to involve finally a penetration of the concrete foundation as well.
6. Summary and recommendations

Since the publication of the second CSNI specialist meeting on molten core-concrete interactions or MCCI (Alsmeyer, et al., 1992) over twenty years ago – a seminal report on the then state-of-the-art, and also the publication of a European Commission state-of-the-art report (SOAR) on the subject matter shortly thereafter (Alsmeyer H., et al., 1995), significant progress has been made in practically all aspects of research and development related to MCCI. The list includes prototypic material experiments at different geometric scales (some approaching quarter scale to full-size reactor cavity geometry), making considerable improvement to the analytical tools (many codes now incorporate far more mechanistic models of the phenomena of interest), and finally performing an increasing number of validation, verification, and benchmarking exercises as well as a much wider plant applications ranging from confirmatory safety analysis to new and advanced reactor licensing. The current SOAR documents this large body of work highlighting the progress made in the last twenty years related to the understanding of concrete ablation and corium coolability mechanisms. This report takes advantage of the SOAR on corium concrete interaction recently published by the SARNET European severe accident network (Cranga, et al., State Of the Art Report on MCCI in dry conditions: analysis of experiments and modelling, 2013).

The state-of-the-art report (SOAR) provides a background discussion of safety issues relevant to core-concrete interactions and melt coolability and related containment failure modes, an overview of various experiment programs that have been carried in the areas of MCCI and debris coolability, a description and assessment of various analytical tools (“codes”) that have been developed to analyse MCCI behaviour and finally, a summary of plant analysis activities that have been carried out using these codes. The report focuses primarily on the progress made in the last two decades on the thermal-hydraulic aspects of MCCI and mentions in passing some early research programs dealing with fission products aspects of MCCI. Finally, the report discusses general aspects of severe accident management (SAM) strategies aimed at achieving melt stabilisation in both generation II and generation III reactors.

It is important to note that in the early days of MCCI research, the focus was mostly on understanding the basic physical phenomena such as heat and mass transfer between various components involved in MCCI (i.e. molten core, coolant, and structural concrete or metal), chemical processes involved in the interaction, transport behaviour of various product species, and the impact of MCCI on containment integrity – all through a combination of experimental and analytical research activities. Much of the experiments in the early days were conducted at small-scale and with simulant materials, and were carefully designed to study individual physical phenomenon in sufficient detail so as to be able to develop models that can analytically replicate the phenomenon at full reactor scale.

In contrast, the majority of more recent MCCI experimental research focused on integral experiments, mostly with prototypic core materials to assess the overall progression and effect of MCCI.

The experimental programs described in Chapter 2 played a major role in improving the understanding of MCCI phenomena and provided a much needed data base for model development.
and validation. Most of the tools, discussed in Chapter 3, are now able to simulate with somewhat simplified assumptions the following behaviour:

- Concrete ablation for different concrete compositions;
- Evolution of corium pool configurations (homogeneous or stratified) as a function of concrete decomposition gas release, density evolution of the oxide and metal phases, as well as oxidation reactions; and
- Different cooling mechanisms when water is added from the top.

The treatment of thermal-hydraulics of a well-mixed corium pool in the presence of the concrete decomposition gases is contrasted by the complexity of the ablation mechanism where the heated concrete, a highly heterogeneous material, is gradually incorporated into the melt through an evolving melt-concrete interface that is still difficult to observe experimentally and capture from a modelling point of view. Because many of the models are not mechanistic, several parameters are empirically fitted to reproduce as best as possible the scaled experimental results. Attempts to model MCCI by a multi-scale computational approach to eliminate these tuned parameters with more mechanistic models have been unsuccessful to date. This is mostly due to the great difficulty of observing and measuring directly the local phenomena needed to validate multi-scale modelling approaches. Nevertheless, in the near future it seems reasonable to reach an intermediate level of complexity to improve existing models. As an example, the recent activities performed by Seiler and Combeau (Seiler & Combeau, 2014) to develop a universal model of transient interface temperature in multicomponent solid-liquid systems should be encouraged.

With regard to cooling mechanisms, top flooding appears to be a practical accident management strategy to achieve enhanced cooling. A boiling heat transfer condition in the absence of other cooling mechanisms at the surface of the corium crust is not enough to significantly slow down basemat ablation. More complex mechanisms play an important role in enhancing heat transfer between the corium and the overlying boiling water pool. Depending upon the melt composition and conditions, four mechanisms have been identified in flooded cavity tests that can contribute to core debris quenching. These mechanisms include: i) bulk cooling in which gas sparging is initially sufficient to preclude stable crust formation at the melt/water interface (and therefore, efficient heat transfer is achieved); ii) water ingress through fissures in the core material that augments what would otherwise be a conduction-limited cooling process; iii) melt (or volcanic) eruptions that lead to a highly porous overlying particle bed that is readily coolable, and iv) transient breach of crusts that form during the quench process, leading to water infiltration below the crust with concurrent increase in the debris cooling rate. Bulk cooling is important as it establishes the initial conditions for the long-term cooling processes that involves crust formation and growth by water ingestion as well as melt eruptions. Transient crust breach is important as it allows water to re-establish contact with underlying core melt, thereby allowing additional cooling to proceed by the water ingestion and melt eruption mechanisms.

While existing data and experiments indicate that debris coolability can be achieved within an envelope that is principally based on concrete type, melt depth, and timing of cavity flooding, this envelope does not encompass the full range of accident conditions that can be encountered in certain plant configurations. Neither does the envelope encompass various abstractions of melt progression in-vessel which give rise to different initial and boundary conditions for ex-vessel melt progression and also, wide variations in concrete constituents within the two major types investigated in the experimental programme and consequent effects of such variations.

The validation work, summarised in Chapter 4, underlines the scaling issues and illustrates the fact that we have reached a reasonable level of confidence in extrapolating experimental conditions to different reactor situations. For full-scale plant safety assessments that are discussed in Chapter 5,
approaches appear more pragmatic whereby MCCI phenomena are analysed based on conservative assumptions with respect to the weaknesses of the containment design.

The fission product release during core/concrete interaction is only briefly mentioned in the report as it has not been the main focus of the MCCI experiments in the last two decades. The aerosols released during corium/concrete interaction contain mainly elements from the concrete. The release of uranium or low-volatile fission product is enhanced by the presence of metal in the melt and by the higher gas content of limestone common sand concrete but remains low. Interaction with silicon to form silicates tends to lower the release of fission products of main interest like barium and strontium.

These various activities, carried out over the last three decades, have significantly increased our level of understanding regarding MCCI behaviour under both wet and dry cavity conditions. Much has been learnt from these experiments in terms of the overall MCCI behaviour at a plant scale. The data gathered has also helped develop coolability models as well as improving existing MCCI models in many stand-alone as well as integral codes. Depending upon containment design, regulatory requirements, and accident management considerations that are unique to each country and reactor type, the current level of understanding in this area is sufficient for conservative reactor safety assessments. Some other important lessons learnt from these activities are highlighted in the next paragraph.

The accident sequences and the possibility of operator intervention result in a broad range of possible initial conditions at time of vessel failure. Following the accident at Three Mile Island and some studies of melt interactions with concrete, it was presumed that core degradation would be very heterogeneous with central regions of the core melting while peripheral regions were barely degraded. Additional core materials would cascade for protracted periods from the reactor vessel as core debris attacked concrete. A certain fraction of the cladding would not be oxidised at the time of core debris relocation to the lower head of the pressure vessel and upon vessel breach there would be a chemical component to the heat generation in the core debris. Additionally, the state of knowledge about late in-vessel melt progression is incomplete (particularly for BWRs). Thus, there is considerable uncertainty regarding the MCCI initial conditions that includes the timing of RPV failure; the initial temperature, mass, and composition of the core debris; the possibility of segregation of metal and oxide melt phases; the pour rate of the melt from the RPV that is determined principally by the melting rate of residual core material, and to a lesser extent by the opening in the RPV lower head; and finally, the timing of water injection (if any).

The remainder of this last chapter highlights the most important remaining issues and residual uncertainties therein, and provides recommendations to address them in the future in order to increase the reliability of reactor simulations. These issues are: (1) long-term core-concrete interaction behaviour; (2) realistic plant simulations; and (3) coolability enhancement under top flooding conditions.

6.1. Long term core-concrete interaction behaviour

The past MCCI experiments with prototypic materials were conducted for a relatively short duration, in part, because the facility constraints did not allow significantly longer duration experiments. Admittedly, in many of these experiments, the relatively short duration was adequate to assure melt stabilisation and slowing down basemat ablation to a level that is considered acceptable for regulatory purposes (i.e. ablation limited to a specified amount by say 24 hours into accident).

The Fukushima accidents suggest that a much longer transient is quite likely. A recent draft report (OECD/NEA, Under preparation) on safety research post-Fukushima by an international group of experts noted that MCCI very likely occurred in Unit 1, and probably as well in Unit 3 for some time, but did not lead to a significant melt release outside the containment vessel to the reactor building. It is not clear if MCCI occurred in Unit 2 but if it did, it would have been after many hours if
not several days following accident initiation. The analyses performed to date in the NEA BSAF project phase 1 have not provided a consensus view on the MCCI issue. In particular, the termination of the MCCI process in Units 1 and 3 as calculated by the different codes (OECD/NEA, 2015) is impacted significantly by differences in melt pour conditions predicted by different codes at reactor vessel failure. The latest results of plant inspections at Unit 1 have provided interesting results; i.e. no significant damage inside the lower containment is visible (Yamanaka, 2015). These findings put into question those analyses results which predict ongoing MCCI for a long time, especially in the presence of water. Hence, there is a need to obtain longer duration experimental data if the shorter duration experimental data cannot be extrapolated to reactor situation with a high degree of confidence.

Longer duration experiments will provide data needed to: (1) confirm that intermittent phenomena like melt eruptions are reproducible; and (2) investigate if the crust formed by water ingression is stable. Long duration experiments will also provide data on long term behaviour dealing with the final phase of the interaction, i.e. the time when the heat flux to concrete is low enough that it can be dissipated by conduction into concrete without further ablation, or the heat flux that is applied in a specific coolant circuit of a core catcher. Finally, long term behaviour also refers to situations wherein the concrete fraction within the melt and the heat flux level are representative of the situation after many hours of interaction (typically, 12 hours or more). The subject of long-term behaviour vis-à-vis further research needs and recommendations will be discussed in the following paragraphs, with particular reference to dry cavity and wet cavity situations, respectively.

6.1.1. Ablation in dry situation

In the past, experiments under dry cavity conditions were run successfully over a period of a few hours. The main insight from these tests was that the ablation profile depended largely on the concrete composition. Based on different experiments and different heating techniques, more rapid radial ablation (relative to axial) was observed for siliceous concrete, whereas LCS concrete showed an isotropic (uniform axial and radial) ablation profile. Since there is currently not an accepted phenomenological explanation for this behaviour, the question remains if it is reliable to extrapolate this result to reactor scale for a longer duration ablation process. The general scaling issue, equally important to ablation in a wet cavity situation, is discussed later in this chapter.

Another more complex issue is associated with intermittent ablation bursts that are observed in experiments; it is not clear if this is a result of crust instability or rather a result of concrete spalling due to mechanical stability. Depending on the phenomena, the characteristic time period can be several hours; e.g. crust dissolution processes with siliceous concrete. In this case the test duration has to be long enough to observe at least two or three ablation bursts. In terms of scaling to confidently reproduce the phenomena that occur at the corium concrete interface, the typical approach has been to preserve the prototypic heat flux to the concrete resulting from fission product decay heat in the core debris. Under these conditions, extending test duration means that the concrete thickness has to be significantly increased.

Also, it is important to recognise that in every facility, the size of the test section, the heating technique, and/or the operating procedure always induces some transient system effects. These transient effects are not modelled in the simulation tools (codes). As a result, when the transient effects are dominant at the experimental scale, the codes cannot reproduce accurately the final cavity shape which is commonly used to estimate the ablation rate. As a recommendation for future test interpretations, we note that the comparison of ablation rates for a given concrete composition cannot be deduced from the final cavity shape, but only from the test periods where both radial and axial ablation reach steady states. As most transient behaviour induced by experiment techniques are not modelled in the simulation tools, reproduction of the final cavity shape is not a legitimate validation goal.
Since the thickness of the concrete walls and the corium mass will always be limited in comparison to the reactor case, to address long term behaviour in future tests it is important to increase the initial concrete fraction in the melt and reduce in a consistent way the power injected to reproduce prototypic long term heat fluxes values. Future tests should also allow be able to run under steady state conditions for a longer duration. In addition, an experimentation objective should be to reduce the duration of the initial transient period leading to the onset of ablation in both radial and axial directions. In this spirit, experiment techniques that can contribute to homogenous initial melting of the corium, as well as limiting initial crust formation during the phase in which the melt initially contacts the concrete (either by pouring or by in-situ melt generation), should be encouraged.

6.1.2. Ablation in wet situation

Core-concrete interaction under wet cavity conditions (i.e. in the presence of an overlying water layer) has been the subject of intense investigation over the last two decades. Testing in this area has focused on systematic investigation of ex-vessel debris coolability. For top flooding, two particular coolability mechanisms – melt eruption and water ingression – were investigated in a number of separate effect tests, latter more so than the former. In the context of long-term CCI, these two mechanisms (or phenomena) are discussed below in more detail with particular emphasis placed on the current level of understanding, knowledge gaps, and recommendations for future research where applicable.

6.1.2.1. Melt eruption mechanism

Experimentally, the melt eruption mechanism appears to be a periodic phenomenon which is currently modelled in a simplified manner in the codes in terms of an average melt entrainment coefficient. The most recent CCI test resulted in melt eruptions even with low gas content siliceous concrete, but the phenomenon occurred only two times during the test that ran for ~ 2 hours. Thus, the extrapolation of an average melt entrainment coefficient deduced from a test of relatively short duration to long-term CCI is inherently uncertain and, as such, should be used with some caution. It is important to conduct longer duration experiments to see if multiple melt eruptions occur. Data from these experiments will reduce uncertainties in current melt eruption models and will provide better confidence in extrapolating to reactor scale.

Under long test operating conditions involving top flooding, one systematic drawback of the experiments is the top crust anchoring phenomena. In some promising tests performed with top flooding, even in some recent ones involving early cavity flooding, the upper crust eventually anchored to the side walls. The anchoring phenomenon unrealistically reduces the efficiency of the melt ejection phenomena because a gap between the pool and the upper crust appears and then increases due to concrete densification upon melting as well as loss of liquid corium as eruptions occur. At the beginning of the process, crust anchoring could also create a pressure build-up effect below the crust that experimentally distorts the eruption process (e.g. promoting an extrusion-type ejection process versus an entrainment process). Crust strength measurements made on samples obtained from reactor material core debris coolability experiments and supporting structural analyses indicate that a floating crust boundary condition is likely for reactor applications involving pit diameters of typically 6 m.

In this spirit, experiment techniques that can promote a floating crust boundary condition in reduced scale experiments should be encouraged. Several attempts have been made in past experiments to reduce the risk of anchoring by relying on the ablating nature of the concrete walls, or to break the crust using a dedicated lance. However, these attempts have been largely unsuccessful. The use of pure mortar for lateral inclined walls with non-intrusive sensors or heating electrodes could be a direction to follow in future tests. The community continues to study novel approaches for achieving a floating crust boundary condition in reduced-scale tests, but these attempts have not been successful to date.
6.1.2.2. Water ingress mechanism

The current approach for modelling enhanced crust cooling by water ingress is based on a critical heat flux concept. The data underlying these models principally comes from SSWICS experiments (see section 2.2) that were transient core debris cooling tests conducted in an inert crucible without sustained heating of the core melt. The results indicate that the critical heat flux depends on crust strength and thermal contraction (which drives crack formation and growth), and the effectiveness of this mechanism rapidly degrades as concrete content in the melt increases. Thus, water ingress cooling is more efficient under early cavity flooding conditions. In terms of extrapolating to reactor scale, the behaviour of this thick cracked crust over a longer duration accident sequence involving subsequent melt pours on top of this material is unknown.

To address this issue, it is important to first realise one experiment limitation; i.e. the solid crust (as well as particulate on top of the crust formed by melt eruptions) is not heated, so that power simulating decay heat is only injected in the liquid phase. This limitation raises the issue of power control during the tests. Different methods are available for heating core debris, but none of them is ideal insofar as mocking up heat produced in core debris by fission product decay. The community continues to investigate heating methods and operational approaches aimed at reducing experiment distortions related to mocking up fission product decay heat in core debris regardless of the debris state (e.g. liquid/solid, continuous/fragmented, etc.).

6.1.3. Termination of the ablation phase

The final step of the MCCI process would occur after several days of ablation in which the core-concrete heat exchange surface becomes so large that the heat can be transferred by conduction to the remaining concrete without further ablation. This scenario would yield a very viscous melt with high concrete fraction. Under such conditions, the heat transfer models at the core-concrete interface may not be valid. Regarding modelling of this long-term behaviour, some simulation tools utilise a quasi-steady modelling approach in which conduction into the concrete is not modelled. Thus, all heat transfer from the core debris is dissipated by ablation, and as a consequence, the ablation never stops.

Some of these deficiencies in analytical tools can be addressed with data from longer duration experiments.

The above situation can be contrasted with dedicated core catcher designs (e.g. EPR or BiMAC) in which the ablation phase in the spreading room is very short and terminated when the heat load from the core melt is balanced by the heat removal in the cooling circuit. In these cases, the melt can remain partially liquid and no more decomposition gases are released from the concrete. Under this condition, the heat transfer mechanism switches to natural convection. To address such situations, specific models are needed to allow a continuous description between a bubbling pool, natural convection, and conduction-limited heat transfer regimes.

Regarding debris formations resulting from top flooding (see Figure 1.3-2 b and Figure 2.4-21), it is important to verify that they are coolable. To compensate the reduction in volumetric heating in the experiment resulting from crust formation, the heat flux at the bottom surface of the upper crust may be increased by increasing input power. However, this same approach would not be viable for formations resulting from melt eruptions that generally consist of volcanic structures with surrounding particle beds. The makeup of the particle beds has been found to consist of 5-10 mm diameter particles that are generally considered to be coolable due to the relatively large particle size. However, the coolability of volcanic structures has not been assessed in the open literature. Because volumetric heating of solid material in MCCI experiments is currently not possible, an alternative approach for assessing the coolability of these structures would need to be undertaken involving analysis possibly supported by well-designed separate effects experiments.
6.1.4. Scaling issue between experiment and reactor situation

The initial mass of corium, shape of the test section, and the number and thickness of the concrete wall(s) used in the experiment are all connected to the issue of scaling test results with respect to time. When the ablation heat flux evolution is preserved in the experiment, corium enrichment with concrete slag occurs more rapidly in 2D experiments than in the reactor situation because the ratio between concrete surface and corium volume ratio is not preserved. For example, in the 2D CCI experiments if a homogeneous power distribution at the pool interface is considered for a 900 MW PWR the corium, then the corium is enriched with concrete about 4 times faster in the CCI experiments than in the prototype (or about 8 times faster in the VULCANO experiments). Conversely, in 1D experiments the scaling is easier as the corium pool is enriched at a prototypic rate if the initial height of the core melt in the experiment is preserved.

One way to conduct a shorter duration test that mocks up longer term behaviour is to reduce to a reasonable extent the height of the corium in 1D experiment, or to increase the core-concrete surface to volume ratio in the experiment. However, in the latter case note that periodic phenomena such as melt eruptions or ablation bursts at any given time will have a more significant impact on the evolution of melt composition. As noted earlier, another way to address long term behaviour is to start the experiment with a higher fraction of concrete in the corium pool and to adjust the heat fluxes to prototypic values. This approach is reasonable for addressing questions related to late phase flooding strategies or to observing the evolution of the melt entrainment rate in a composition range corresponding to long term scenarios, despite the crust anchoring drawback.

6.2. Realistic plant simulations

Improving the realism in plant simulations inherently introduces more complexity. As a result, these associated efforts have to be balanced with approaches that rely on invoking additional levels of conservatism to define a bounding set of hypotheses for safety-relevant issues. Three major topics of interest in this regard are:

- The presence of metal within the melt or within the concrete;
- The initial conditions for MCCI based on melt pour conditions into the reactor pit;
- The presence of impurities in cooling water (e.g. seawater or brackish water).

Additionally, it is noted in passing that thermal stresses on concrete structures brought on by core debris interactions with concrete have not been investigated in MCCI Programs. These stresses are largely inconsequential for below grade reactor cavities but can be quite important for free standing cavities such as sub-atmospheric containments and especially for reactor pedestals in boiling water reactors. The core debris interactions place the inner region in compression where concrete is strong but the outer region in tension where concrete is weak and easily cracks. This has structural implications which again have not been investigated in MCCI Programs.

6.2.1. The presence of metal within the melt or within the concrete

In the reactor configuration, some metallic elements are always present within the melt. Depending on the reactor situation and ongoing scenario, they can come from different sources that include the initial melt pour, as well as sources within the containment that arise through ablation; i.e.

- Metallic melt constituents (primarily U and Zr, but also control rod, blade, channel box, and canister materials) transferred as part of the melt pour at vessel failure, or during successive pours;
- Steel coming from the vessel or remaining structures that is heated and melted by the radiative flux from the corium in the reactor pit before top flooding; and
Steel coming from the reinforcement in the concrete basemat and/or sidewalls when the concrete is ablated, or from the containment liner (where applicable) when contacted by core melt.

6.2.1.1. Impact of metal on concrete ablation profile

The presence of an immiscible metallic phase within the melt influences the ablation profile as soon as stratification occurs. This phase is mainly composed of steel as zirconium and uranium elements are rapidly oxidised. During this short term period, zirconium and uranium affect the density of the metallic layer and their oxidation produces a significant heat of reaction increasing the initial ablation rate. Then, or after the last pour from the reactor vessel, the metal phase composition is predominately steel. In the metal layer, the heat flux to the concrete is higher because: i) the layer is not diluted by the melted concrete (the metal coming from the reinforcement in the concrete is usually consumed by oxidation), ii) the heat transfer between oxidic and metallic layers is high due to gas bubbling (interface dispersion and induced convection), and iii) the high thermal conductivity of the metal phase increases the heat transfer coefficient to the concrete.

The stratification process is governed by the higher density ratio between metal and oxides as soon as the fuel oxides become diluted with concrete oxides, as well as the decrease in melt gas sparging rate that generally occurs during the course of the MCCI. Gas sparging initially promotes mixing of the metal and oxide phases, whereas the oxidation reactions leads to metal disappearance and thus a gradual return to a homogeneous configuration.

While several experiments have been performed with iron-alumina thermite simulant, only a few VULCANO tests have been performed with a prototypic metal-oxide core melt composition. It was not possible to establish from the VULCANO results clear evidence of stratification but ablation was observed to be increased in front of the metallic masses. Moreover, the results indicate that metals oxidation is not only driven by gas liberation from the ablated concrete, but also from gas liberated from the bulk of the concrete material heated by conduction behind the ablation front location. For prototypic metal-oxide core melt, more generally the oxidation kinetics and the stratification thresholds are important as they influence the time window when the melt is stratified at reactor scale, and as a result the prediction of the basemat melt through time.

The initial phase of the interaction involving unoxidised cladding (zirconium) in the melt has been investigated in a few reactor material experiments. This stage can lead to highly exothermic metal oxidation reactions. Zirconium-bearing concrete-metal inserts were used in some Argonne experiments in which a relatively small amount of Zr was incorporated into the melt just prior to melt contact with the concrete basemat. However, it is likely that a significant fraction of the Zr in the inserts was oxidised before the test was initiated, thereby limiting the impact of this metal on the actual MCCI phase of the experiment.

Another aspect not investigated in experiments is the presence of uranium within the metallic phase. During the in-vessel stage of the accident, uranium is found in the metal phase in scenarios that lead to a significant fraction of unoxidised cladding in the lower head. The presence of the cladding results in the formation of a heavy U-Fe-Zr metallic phase at the bottom of the oxide melt in the lower head. During MCCI, it is likely that this metal layer will stratify near the beginning of the interaction. Under these conditions it seems appropriate to implement an oxidation model for uranium in simulation tools and to perform sensitivity analyses.

1. Additionally the distribution of the spatial power in between oxidic and metallic phase remains uncertain in the VULCANO experiments.
Reinforcing bars in concrete play a double role as they are a continuous source of metal during ablation, in addition to changing the ablation mechanism. In particular, recent MOCKA test results using simulant materials indicate that the presence of reinforcing bars in siliceous concrete leads to a homogeneous ablation profile, which contradicts the results from reactor material tests carried out with non-reinforced siliceous concrete in which anisotropic ablation is observed (Foit, Fischer, Journeau, & Langrock, 2014). One hypothesis is that the rebar increases the mechanical stability of dehydrated concrete, thereby preventing gross failure and ablation bursts that could cause ablation anisotropy. In contrast, for limestone common sand concrete the ablation profile in MOCKA tests is anisotropic (larger sidewall ablation) and not modified by the concrete reinforcement (Foit J. J., 2015). This trend is also observed in the VBSU-1 reactor material test carried out with non-reinforced limestone common sand concrete (Foit, Fischer, Journeau, & Langrock, 2014). Running experiments with reinforcing bars embedded in the concrete means that we first have to avoid short circuiting electrical heating methods or introducing non-prototypic direct heating issues. In terms of scaling it is important to consider a test section large enough to respect the size of the aggregate and the spacing between the rebar and conduct the test over longer duration to reach a steady state.

6.2.1.2. Effect of metal on cooling mechanisms

Since the water ingression cooling mechanism depends on crust mechanical properties, the presence of metallic inclusions in an otherwise oxidic crust could change the properties and thereby impact this mechanism. Specifically, the presence of metal could influence the critical heat flux associated with cracks that form in the crust due to thermal contraction induced by top flooding. To address this issue, additional SSWICCS-like experiments could be performed with different metal contents in the melt and a representative gas release to promote good mixing conditions. Some tests of this type are already scheduled in the frame of the MIT3BAR Program (Journeau & Teisseire, 2015). For these tests, as well as large scale experiments with sustained heating, new thermite compositions need to be developed that would produce a melt with adequate metal fraction. An alternative would be to add metal after the initial melting phase of an otherwise oxidic melt. Metal-oxide mixing is important since, without it, the situation will be very similar to pure oxidic melt conditions. It is not necessary to address the behaviour of a purely metallic crust because the water ingression mechanism is closely linked with early flooding and in that case the pool is expected to be well mixed.

The situation for the melt ejection cooling mechanism is different as it occurs over a longer duration. The melt could be mixed at the beginning with a priori no major differences, and then after stratification occurs the ablation rate will increase with a corresponding increase in the gas release rate. After most of the oxide melt is ejected, the ablation rate would decrease again and the remaining liquid would tend towards a purely metallic layer with a low viscosity. For a floating crust, a part of the metal layer could ingress in the upper crust and remain heated. It could be interesting even if it is not of a high priority to evaluate the entrainment rate of pure metal melts and check the morphology of the particles formed during quenching to assess their coolability as well as their influence on the coolability of the debris bed in general. As soon as the specific technological challenges of metal-oxide experiments are resolved, tests with a high metal fraction could be performed in order to reach conditions in which most of the oxide is included in the upper crust and particle bed regions.

6.2.2. The initial conditions for MCCI or the pouring phases from the vessel to the reactor pit

This section provides a discussion of the expected MCCI initial conditions for realistic reactor scenarios. For the purposes of simplification, it is often assumed that the MCCI phase starts as soon as

2. This is a challenge for the MOCKA experiments where the decay heat is simulated by adding chemically energetic mixtures to the melt.
the vessel fails and the corium mass in the lower head (which in bounding analyses includes the entire fuel and structural inventory in the reactor) is relocated into the reactor pit. This approach offers a degree of conservatism in terms of axial-melt-through delay if one assumes that the melt is spread instantly over the entire surface of a dry pit. However, when the pit is flooded spreading may be limited, leading to corium accumulation in one part of the pit. This will result in higher heat fluxes to concrete and reduce the basemat melt through time if this accumulation is stable and does not eventually spread out uniformly. Among other things, this situation depends on the corium temperature, pour rate, failure location, amount of water present at vessel failure, and finally on the ability to provide water continuously on top of the corium accumulation.

Such configurations are quite complex to study because they involve the formation and spreading of corium accumulations under water as well as the possibility of boiling off the water inventory, drying out the core debris, re-melting, and onset of concrete ablation. An ancillary issue is that core debris in a reactor cavity, if not covered by water, exposes a great deal of concrete surface area to intense convective and radiative heat flux. The gas generation and concrete degradation from this exposed concrete cannot be neglected in the analysis of core concrete interactions and containment integrity.

Depending upon the melt pour conditions and with a relatively shallow water layer, melt jet fragmentation is expected to be minor. For this type of scenario, existing MCCI models that treat the corium as an initially intact melt pool interacting with concrete may be employed as a reasonable approximation. However, for deeper water pools melt jet fragmentation may be significant, leading to formation of a coherent particle bed, or a compact melt layer commonly referred to as a cake surrounded by particle bed (see FARO (Tromm, Foit, & Magallon, 2000). Depending upon the bed depth, decay heat level, particle size, and porosity, the configuration may be coolable. However, if the dry-out limit for the bed is too low then gradual reheating, dry-out, melting, and onset of concrete ablation will occur. These particular configurations have not been extensively investigated as part of MCCI research, nor can existing MCCI models address this type of behaviour. However, there has been a significant amount of research done in the area (both experiments and modelling) that generically addresses debris bed coolability for both in-vessel and ex-vessel applications. Obviously, conducting experiments that involve dry-out and melting of particle beds composed of reactor materials is a technical challenge given limitations with current core debris heating techniques (see above discussions). Thus, a possible first step to address this issue is to utilise existing models to evaluate coolability of particle bed formations predicted for plant applications. If these analyses indicate that the beds are likely to be uncoolable, then effort should be devoted to developing appropriate experiment techniques to address this type of behaviour.

Another related issue is that of multiple pours and how that affects the coolability of debris in the reactor pit. Again, in all experimental and analytical studies concerning MCCI, it is traditionally assumed that at vessel failure, the molten material (whether the entire reactor inventory or partial inventory) is ejected all at once and spreads on the reactor pit surface. It is likely that in some accident scenarios, the melt pour would be periodic which has two consequences: non-uniform melt accumulation and non-uniform spreading. Conducting an experiment with this kind of melt configuration may be quite challenging, and an analytical extrapolation of experimental data for symmetric and uniform melt configuration may be more worthwhile based on simulant data.

6.2.3. Presence of impurities in cooling water

The impact of impurities in cooling water on severe accident behaviour resurfaced following the Fukushima accident. In particular, the use of sea water brought into question the effect of salt (sodium chloride) on coolability mechanisms, the impact on the chemistry of fission products, and the performance (i.e. potential for clogging) of coolant loops. Generally speaking, any impurity in cooling water (whether it is salt in sea water or other forms of impurities in brackish inland fresh water) can impact one or more of these areas.
For the cooling mechanisms identified under top flooding conditions for a corium pool interacting with concrete, the formation of precipitate in the cracks of the upper crust or in the overlying debris bed could reduce the dry-out limits for these formations. As the composition of water present in the sumps at the bottom of the reactor building is complex and can depend on the accident management strategy, it seems easier to address the issue in separate effect tests than in semi-integral experiments. Ongoing experiments in Japan are addressing some aspects of the water impurity issue (see Appendix 7.3). To parametrically investigate the effect of water impurities on coolability by water ingestion, SSWICS-like tests could be run to evaluate the impact on the cracks formation and on the crust critical heat flux. If warranted, more complex experiments (i.e. with sustained heating) could be conducted to assess the behaviour over the long term.

For the melt ejection mechanism, the influence of impurities on debris bed coolability could be investigated in separate effects tests that utilise existing facilities that are studying dry-out in debris beds for in vessel conditions in order to assess the long term coolability.

The water at the bottom of the containment building will be highly contaminated with fission products. If this water is used to cool the melt, the chemistry of the fission products will likely be modified by gas bubbling and more generally by particulate entrained in the water. While the fission product behaviour under such conditions is an ancillary issue related to the consequences of clogging, water samples could be collected quite easily at the end of MCCI experiments to perform chemical analysis in order to characterise the chemical composition. If some impurities in the water can play a role in trapping other species released during MCCI, it would be useful to carefully select the initial composition of the water before running these tests.

6.3. Coolability enhancement under top flooding conditions

The SOAR is focused on ex-vessel coolability under the top flooding which is largely regarded as a generic accident management strategy for ex-vessel melt stabilisation in existing plants.

The improvement of melt coolability under top flooding conditions can also be viewed as a potential back-fitting strategy for operating reactors. Moreover, for new reactor designs spreading and top flooding can be incorporated in the design phase as a generic approach.

In this regard, the first point is that a larger initial corium spreading area will reduce the downward heat flux to the concrete. This approach can be used for a new design if the basemat is thick enough. One approach for increasing spreading area for plants with limited floor space is to allow radial melt-through of a barrier with subsequent spreading of a portion of the melt into the reactor building. This situation is more likely for siliceous concrete but remains limited only to the level of corium above the breach elevation. Referring to the presentation in Chapter 5 about the Fessenheim plant in France, modifications like an unplugged, dedicated hole in the wall of the reactor pit can promote additional spreading in a dedicated dry room. This spreading will be easier and faster in a dry cavity situation, one that also provides the benefit of eliminating the risk of steam explosion.

The coolability of debris can be more efficient if spreading is combined with water flooding. In the EPR and BiMAC core catcher designs, water cooling circuits located at the bottom are provided to achieve this objective. For existing reactors, coolability is achieved mainly by top flooding as part of the accident management strategy. Two methods can be used to improve the coolability under these conditions:

- Early flooding to capitalise on the fact that the water ingression mechanism is most efficient at this stage with little concrete present in the melt, and that the melt eruption mechanism is also most effective in the early phase of the corium-concrete interaction due to the higher melt gas sparging rate;
- High gas content in the concrete to maximise the impact of the melt ejection cooling mechanism over the long term and accelerate the oxidation of the metallic phase.

The first point is closely connected to the SAM strategy. Ideally, it is desirable to have an initially dry pit to maximise spreading and to avoid the risk of a steam explosion, followed by early flooding. In this case, the time window to add water is narrow and a subsequent melt pour after top flooding cannot be excluded.

The second point depends on the composition of the concrete that is used for the reactor basemat and so on the plant location. If a recommendation is made to consider high carbonate and/or hydrate contents for the concrete of new reactor basemat, a back-fitting measure for plant having a potentially too thin siliceous concrete basemat could be to consider pouring an additional (sacrificial) layer of such a concrete. In this case, since the thickness of this additional layer is obviously limited, the key piece of information needed is the efficiency of the melt ejection mechanism so as to ensure that all the liquid portion of the melt is transformed into a coolable debris bed before reaching the original siliceous concrete.

In the coming years the examination of the debris in the three damaged Fukushima reactors will likely provide additional insights that will enhance the understanding of MCCI phenomena at large scale and under fully prototypic conditions. The findings will undoubtedly provide additional confidence in the application of simulation tools to existing plants. They will also aid in optimising SAM strategies for existing as well as future plants. In the meantime, in order to perform experiments and additional analysis to address more realistic situations, it will be necessary to improve the capabilities of existing facilities to overcome the technological challenges that have been described in this chapter.

One of the top level recommendations in the NEA-SAREF report (in preparation) is to organise an MCCI workshop to discuss current state of MCCI knowledge, identify knowledge gaps, and identify data needs to bridge the gaps – the idea being that the Fukushima decommissioning effort can be informed by the outcome of such a workshop while at the same time, data collected during the decommissioning activities can be optimised to bridge the MCCI knowledge gaps. In two companion studies (one on severe accident knowledge gaps post-Fukushima and the other on Fukushima forensic data needs), MCCI knowledge and data gaps were identified as high priority topics. These findings confirm that in order to perform experiments and additional analysis to address more realistic situations, it is necessary to improve the capabilities of existing facilities and to perform needed experiments to bridge the knowledge gaps and reduce residual uncertainties. Since experimental MCCI research with prototypic reactor materials is an expensive undertaking, a collaborative effort among various nuclear safety research organisations in different countries is highly recommended.
7. Appendices

7.1. Appendix on detailed code descriptions

This appendix provided a detailed description of most of the codes presented in the Chapter 3.

7.1.1. COCO code

7.1.1.1. Pool interface models

7.1.1.1.1. Pool/concrete interface model

Crust and slag (molten concrete components) are considered between concrete and melt as shown in Figure 7.1-1.

The crust is a mixture of corium and calcined concrete components and it includes not only solid but also fluid that does not move due to high viscosity. The temperature of crust surface (interface between melt and crust) is determined so that the solid volume fraction of the melt at the interface will become a prescribed value, which is input data of COCO and usually given as 0.74 (density in close packing of equal spheres).

The growth of the crust is calculated by the balance of heat transfer from the melt and heat conduction to ablation front through crust and slag layer. The ablation rate is also calculated by the balance of the heat flux from slag layer and heat conduction in concrete (the temperature at the ablation front is given as the melting point of concrete).

7.1.1.1.2. Pool upper interface model

The interface model between top crust and melt pool is the same as that of the crust on concrete. The temperature of crust surface is determined so that the solid volume fraction of the melt at the interface is equal to a prescribed value.)

7.1.1.2. Thermal-hydraulics models

7.1.1.2.1. Heat convection within a pool layer

The convective heat transfer rates from melt to surrounding boundaries are calculated with the following correlation:
\[
    Nu^m = \left\{ a_e^m + a_i^m \left( \frac{g \ell^3 \beta \Delta T}{v^2 Pr} \right)^{nt(m)} \right\} + a_b^m \left( \frac{g \ell^3 \alpha}{v^2 Pr} \right)^{nb(m)} \]

Equation 7.1-1

Where,

- \( Nu \): Nusselt number,
- \( a_e, a_i, a_b, nt, nb \): constants,
- \( g \): gravitational acceleration,
- \( \ell \): characteristic length,
- \( \beta \): Thermal coefficient of volumetric expansion,
- \( Pr \): Prandtl number,
- \( v \): Kinematic viscosity of melt,
- \( \alpha \): Void fraction in melt,
- \( \Delta T \): Temperature difference (melt temperature – boundary temperature)

Superscript \( m \): direction (basemat, sidewall or top crust)

The first term comes from heat conduction effect in melt, the second term expresses the heat transfer effect due to the thermal natural convection and the third term is the agitation effect by gas bubbles. The constants \( a_e, a_i, a_b, nt, nb \) are input data to the COCO code and determined by the calculation by CFD code including the natural convection model and gas-liquid two-phase flow model. The heat transfer coefficient is not zero even in the limit of \( \Delta T \to 0 \) or \( \alpha \to 0 \). Indeed the correlation is expressed by the addition of three independent terms.

7.1.1.2.2. Heat convection between oxide and metal layers in a stratified pool

The heat transfer coefficient between oxide and metal layer is assumed to be so large that oxide and metal temperatures are the same.

7.1.1.2.3. Stratification criteria

The choice of stratification model is decided by the user. When the ratio of oxide to metal density is small, the stratification model will be used. For example, an experiment using molten stainless steel pouring on to concrete cavity showed that the calculation with the stratification option agreed with the experimental results, because the density of molten concrete is far less than molten stainless steel.

7.1.1.3. Thermophysical properties

The viscosity of melt liquid phase is calculated by Kendall-Monroe (Kendell & Monroe, 1971), Shaw correlation (Shaw, 1972) or a correlation derived for basaltic concrete

7.1.1.4. Thermo-chemical properties

The melt, which is a mixture of corium and concrete, is a two phase fluid of liquid and solid. In the COCO code, the solid fraction of the melt is determined according to a phase diagram (see Figure 7.1-2).

![Figure 7.1-2: An example of phase diagram in COCO code](image-url)
The phase diagram data are calculated with thermodynamic database (Fukasawa, Tamura, & Hasebe, 2005). One example of the phase diagram for corium and siliceous concrete system is shown in Figure 7.1-2.

The calculated results of solid fraction and corium fraction in solid at various concrete contents and temperatures are tabulated and COCO utilises the table data to determine the solid fraction in melts and the effective viscosity of the melt. The effective viscosity of the liquid-solid mixture \( \mu \) is determined by Stedman’s correlation (Stedman, Evans, & Woodthorpe, 1990) with the volume fraction of solid \( \phi \):

\[
\mu = \mu_m \frac{1 + \phi/2}{(1 - \phi)^2}
\]

where \( \mu_m \) is the viscosity of the liquid.

### 7.1.1.5. Coolability models

When there is no water at the top of the melt, heat transport by radiation from surface of melt or crust top surface is considered. The view factors from melt surface to upper sidewall and ceiling are calculated and all surfaces are assumed to be grey body. The temperature of the ceiling is a boundary condition and given by input.

When there is water above the top of the melt, cooling effects of water ingress and melt eruption are considered. One-dimensional computation grid is created in the crust (number of mesh increases when the crust grows) and the composition and temperature are evaluated at each mesh point. The composition of the crust is decided depending on the melt composition when the crust is formed. The dry-out heat flux at each mesh point is calculated by a correlation derived by SSWICS test in OECD/MCCI Project (Lomperski & Farmer, 2007) and the maximum cooling rate is limited by the dry-out heat flux at each mesh point.

The melt eruption rate is calculated by the following equation,

\[
J_m = K_e J_g
\]

where \( J_g \) is the superficial velocity, subscripts \( m \) and \( g \) denote the melt and gas, and \( K_e \) denotes the entrainment coefficient, which is calculated by the following correlation.

\[
K_e = E \left( \frac{\rho_g}{\rho_m} \right)^{1/2}
\]

where \( \rho \) is a density

The proportionality constant \( E \) is an input data.

The debris erupted above the crust is assumed to be sufficiently cooled by water.

### 7.1.1.6. Code simplifications and limitations

- In COCO, axisymmetric cavity is assumed. Appropriate modelling is required for the analysis of non-axisymmetric cavity, e.g. PCV floor drain sump pit of BWR.
- Additional melt pouring is not considered.
- Calculation of melt spread is out of scope.
• The mass released by melt eruption is excluded from the calculation. Because, the erupted melt is assumed to be cooled by covering water in the form of debris bed.
• The calculation is terminated when all the melt is solidified.
• When the melt is cooled and the solid fraction increases, the melt shows Bingham plastic behaviour. In COCO, the melt is assumed to be Newtonian fluid; therefore the simulation is not exact for crust formation near the ablation front and for the heat transfer rate in the high solid fraction condition of melt.
• In COCO, the exact correlation of boiling curve is not used in the calculation of rewetting timing after water flooding. Instead, rewetting is determined when the heat flux at the top of the crust falls below the dry-out heat flux of the crust. On the other hand, the surface of the crust will be uneven and there will be cracks and voids which reduce the heat conduction in the crust, therefore the temperature and heat flux at the crust surface will not be uniform and local quenching will occur at low temperature point and the rewetting will spread all over the crust surface from this point. Therefore, the rewetting heat flux in COCO is assumed equal to crust dry-out heat flux.

7.1.2. **CORCON code**

7.1.2.1. **Pool interface models**

Heat is removed at the boundaries of the pool, which are its top surface and its interface with concrete. The internal temperature of the pool adjusts quickly so that these heat losses balance the internal heat generation, and the heat transfer approaches a steady state. The heat transfer model allows for several possible configurations in each layer: the layer may be completely molten, it may have a solid crust on one or more surfaces, or it may be completely solid.

7.1.2.1.1. **Pool/concrete interface models**

Heat loss from the top of the molten debris is dominated by radiation to containment structures or to the overlying water. Because of the fourth-power temperature dependence of the radiative flux, this loss is rather insensitive to containment temperatures (unless they are very high). The decrease in radiative heat transfer from the pool surface to the surroundings, associated with atmospheric attenuation by aerosols, is approximately accounted for. **Pool/concrete interface model**

Heat transfer between molten core debris and reactor cavity concrete is controlled by the bubbling of concrete decomposition gases through the melt. This process is similar to nucleate boiling or gas sparging except that at the interface between the core debris and the concrete, gas is being released coincident with melting of the concrete surface. Coincident with gas bubbling and concrete melting at the interface, the molten core debris may begin to solidify as a crust adjacent to the melting concrete surface. This crust may be stable or unstable depending on its growth rate, its strength properties, and the disruptive forces acting to destabilise it.

**CORCON** has two models for interfacial heat transfer: gas film model and slag film model. At extremely high gas generation rates, it may be possible to form a stable gas film at the melt-concrete interface. When a stable film is present, heat transfer across the film is by combined radiation and convection. For a stable gas film on a nearly horizontal surface, heat transfer is computed from a mechanistic model based on momentum balance in a Taylor-instability bubbling cell. The result may be cast in the form of a Nusselt number based on film thickness. For an inclined surface, a flowing film model is used and both laminar and turbulent films are considered. CORCON-Mod3 employs a simple transition between the laminar and the turbulent flow regimes to ensure continuity of film thickness and heat-transfer coefficient with the appropriate limits. In all cases, Nusselt-type correlations are used.
The simplified model for the concrete response in CORCON-Mod3 is based on a steady-state, one-dimensional energy balance. For a steady temperature profile in the concrete, a simple heat balance at the concrete surface provides information on concrete erosion (ablation) in terms of net heat flux to concrete and its ablation enthalpy. The steady-state model calculates the generation of decomposition gases. The ablation enthalpy for concrete is calculated internally, and consists of both sensible and chemical energies. The sensible energy includes the energy necessary to raise gaseous decomposition products to the concrete ablation temperature. The chemical energy is included using experimentally determined heats of decomposition for three reactions: evaporation of free water, release of chemically bound water from hydroxides, and release of carbon dioxide from carbonates.

The ablation temperature of concrete is not precisely defined because ablated material may not be completely molten. In CORCON-Mod3, a melting range is defined by the concrete liquidus and solidus temperatures, with the ablation temperature ordinarily chosen by the user to lie between them. The choice affects the calculated heat of ablation. If the concrete contains reinforcing steel, the energy necessary to raise it to the concrete ablation temperatures is included in the concrete ablation enthalpy.

7.1.2.1.2. Pool upper interface model

Heat loss from the pool surface includes convective heat transfer to the atmosphere and radiative heat transfer to the surroundings. Thermal radiation is the dominant mechanism. If desired, the radiative effects of aerosols in the atmosphere may be included in the calculation, with an atmospheric opacity determined from calculated aerosol concentrations. Once the optical thickness of the atmosphere is known, the one-dimensional diffusion equation is applied for infinite, parallel, and optically grey plates. Convection produces additional heat transfer from the pool surface. Unless the atmosphere is truly transparent, however, convection and radiation are strongly coupled; the radiation tends to increase thermal stability and reduce convection. In most cases, convection is found to be small so only a simple convection model is included in CORCON-Mod3.

If water is present, it will form an additional layer at the top of the pool. This is likely to cool the top of the melt below the solidification temperature, resulting in a solid crust on the surface. The crust will progressively fragment and allow water ingression until the core debris is completely quenched. An overlying coolant pool will also trap aerosols generated during the core-concrete interaction. CORCON-Mod3 treats heat transfer to the coolant using pool boiling correlations. It does not allow for either steam explosions or the progressive quenching of a molten pool to a coolable debris bed.

7.1.2.1.3. Crust formation and freezing

CORCON-Mod3 has a relatively simple quasi-steady-state model for crust formation and freezing. The model is formulated in terms of the average temperature of the layer, which is known from its mass and energy content. The average temperature and the boundary heat fluxes at this steady state are determined by the internal heating and the boundary temperatures for the layer. As a further simplification, the problem is reduced to two independent one-dimensional problems, one axial and one radial, by performing radial and axial averages, respectively, of the full two-dimensional problems.

Within a one-dimensional calculation, a layer may be entirely liquid, entirely solid, or liquid with a solid crust. For the axial case, a crust may exist on the top, on the bottom, or both. In liquid regions, heat transfer is by convection (natural or bubble-enhanced) with a conduction limit. In solid regions, it is by conduction. In the case of a liquid with crusts, the liquid layer is solved first using assumed values of its average temperature and thickness or radius. For any surface at which a crust exists, the boundary temperature is assumed to be the solidification temperature.
7.1.2.2. Pool thermalhydraulics

7.1.2.2.1. Heat convection within a pool layer

The convective bulk pool heat transfer model in CORCON-Mod3 has evolved from those used in CORCON-Mod1, and CORCON-Mod2. For the bottom interface of the melt pool, where gas bubbles may be injected from the incoming concrete, the heat transfer coefficient for a liquid layer is calculated using the Kutateladze correlation (Kutateladze & Malenkov, 1978). For many fluids, the transition velocity calculated using the Kutateladze correlation is comparable to the velocity for transition from bubbly to churn-turbulent flow. For the upper interface of the uppermost melt layer, the Kutateladze correlation is simply multiplied by an area enhancement factor derived by Farmer.

For heat transfer between liquid layers within the melt pool, Greene’s correlation (Greene & Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988) is used to calculate the heat transfer coefficient in each layer.

As time progresses, the debris pool grows; its surface area increases, and decay heating decreases. Therefore, pool temperatures and heat fluxes decrease, and the possibility of refreezing arises. Substantial freezing of the metallic phase may occur. However, the large internal heating and small thermal conductivity of the oxidic phase prevent the existence of steady crusts more than a few centimetres thick. The bulk of this phase will remain liquid, probably for weeks.

7.1.2.2.2. Heat convection between oxide and metal layers in a stratified pool

CORCON models the melt pool as consisting of a number of layers contained in a concrete cavity. These layers are, from bottom up, a heavy (i.e. dense) oxide phase (HOX), a heterogeneous mixture of heavy oxides and metals (HMX), a metallic phase (MET), a heterogeneous mixture of metals and light oxides (LMX), a light oxide phase (LOX), a coolant (CLN), and the atmosphere (ATM). Mixing, stratification, and density changes resulting from material addition can lead to changes in the layer orientation during a calculation.

For a multi-layered pool, CORCON-Mod3 considers heat transfer one layer at a time. Given a trial set of interfacial temperatures, a solution is found for each layer. Newton’s iteration is then used to revise the interfacial temperatures to satisfy the requirement that the heat flux must be continuous at all interfaces between layers. The heat transfer model allows for several possible configurations in each layer: the layer may be completely molten, it may have a solid crust on one or more surfaces, or it may be completely solid. For the bottom interface of the melt pool, where gas bubbles may be injected from the incoming concrete, the heat transfer coefficient for a liquid layer is calculated using the correlation devised by Kutateladze.

As time progresses, the debris pool grows; its surface area increases, and decay heating decreases. Therefore, pool temperatures and heat fluxes decrease, and the possibility of refreezing arises. Substantial freezing of the metallic phase may occur. However, the large internal heating and small thermal conductivity of the oxidic phase prevent the existence of steady crusts more than a few centimetres thick. The bulk of this phase will remain liquid, probably for weeks.

7.1.2.2.3. Stratification criteria

Experimental evidence (Bradley, Gardner, Brockmann, & Griffith, 1993) shows that the various oxidic species in the melt are highly miscible, as are the metallic species, but that the two groups are mutually immiscible. In the absence of gas bubbling, the core debris will stratify into distinct layers based on their relative densities. Mixing of the immiscible layers can occur at high gas fluxes or when the densities of the layers are close. During a core-concrete interaction, there may be times in which the molten core debris is mixed and other times in which the debris is stratified.

In CORCON-Mod3, the user has the option of selecting whether to begin a calculation with a stratified or a fully mixed debris pool, and the user can select whether the code will perform the entrainment and de-entrainment calculations. This allows the user the flexibility to begin a calculation...
in a fully mixed configuration and then allow the code to calculate entrainment and de-entrainment, or the user can force the debris pool to remain mixed by bypassing the mixing calculation. Similarly, the user can begin a calculation with a stratified debris pool and then allow the code to calculate entrainment and de-entrainment, or the user can force the pool to remain stratified by bypassing the mixing calculation. Mixture layers stratify into distinct metal and oxide layers if density differences become greater or the gas flow through the melt decreases.

7.1.2.3. Thermophysical and thermo-chemical properties

Calculation of the physical processes described in the preceding sections requires values for a wide range of thermodynamic and transport properties. These properties are treated as functions of composition and of temperature. However, the dependence is not always explicitly included in the CORCON-Mod3 model; for example, the values used for surface tensions are independent of temperature. In most cases, properties are required for the mixtures of species which make up the components of the CORCON system, although the calculation of chemical equilibrium requires the chemical properties of the individual species. With a few exceptions, such as the viscosity of oxide mixtures with large silica contents, mixture properties are calculated from those of the constituent species. A user option for specifying the phase diagrams of the metallic and oxidic phases is provided in CORCON-Mod3. Also, properties used by the CORCON-Mod3 implementation of the VANESA (Powers, Brockmann, & Shiver, 1986) model are included.

7.1.2.3.1. Thermodynamic properties

The thermodynamic properties calculated are density, specific heat, enthalpy, and chemical potential. Mixture densities are computed from the molar volumes of the individual species. The temperature range of the data from which density-molar volume relationship is generated varies considerably for different species. For many of the oxides, it is from 1 200°C to 1 800°C, while for others it covers the entire range from melting to boiling. The melting range, defined by the liquidus and solidus temperatures, is prescribed by external models for mixtures of condensed species.

The specific heat of any species, condensed or gaseous, is represented in the form of a polynomial function of temperature. A single range of temperature is used for all gaseous species, and the fits are valid from 25°C to approximately 5 700°C. The fits for condensed species include both the liquid and one or more solid phases. Mixture specific heats are computed by mass averaging of component specific heats. Specific enthalpies are computed from integrals of the corresponding specific heats. As with the specific heat, the enthalpy of a mixture is computed by mass averaging. Chemical potentials for the species are required in the calculation of chemical equilibrium and are computed from the molar Gibbs function. The VANESA model tabulates and uses free energy data in the form of free energy functions which are related to the Gibbs function and enthalpy.

7.1.2.3.2. Transport properties

The transport properties computed in CORCON-Mod3 are the dynamic viscosity, the thermal conductivity, the surface tension, and the emissivity. Detailed models are included for condensed phase species and mixtures only. Gas-phase viscosity and thermal conductivity (required for calculation of heat-transfer coefficients at the melt/concrete interface) are treated as constants using representative values. The viscosity of molten oxides is quite complex, particularly, when significant amounts of silica are present. For low-silica mixtures, the viscosity is computed from the Kendall-Monroe expression (Kendell & Monroe, 1971). The viscosities of the species are determined using an Andrade equation. For mixtures with higher silica content, the viscosity can be greatly increased by the formation of strongly bonded chains of SiO4 tetrahedral. The viscosity is calculated from a model proposed by Shaw. This was originally generated as a fit to the correlation developed by Bottinga and Weill (Bottinga & Weill, 1972). The viscosity of the metallic phase is assumed to be represented by
the viscosity of iron (the major constituent). The coolant viscosity is computed by the standard formula. CORCON-Mod3 contains a model for the enhancement of viscosity by suspended solids.

Values for thermal conductivity for condensed phase species do not include any temperature dependence. Mixture values are computed from the species values by mole-fraction averaging. CORCON uses the same thermal conductivity for both the solid and liquid. This is a fairly reasonable assumption for the oxide phase. The code has the provision for specifying a multiplier to be applied in the calculation of the metal phase thermal conductivity. This modified thermal conductivity applies to both the solid and liquid phases. Like thermal conductivity, values of surface tension for condensed phase species do not include any temperature dependence. Mixture values are computed from the species values by mole-fraction averaging.

The calculation of radiative heat transfer requires emissivities for the radiating surfaces. In the CORCON-Mod3 code, only the emissivity of water (coolant) is stored as internal data. Values are input by the user for the ablating concrete surface, for the oxidic and metallic melt phases, and for the surroundings above the pool. The first is specified as a constant, while the last three may be input as functions of either surface temperature or time.

7.1.2.3.3. Chemical reactions

In CORCON-Mod3, both the reactions of metals with gases from the concrete and the condensed phase reactions between oxides and metals are modelled. Condensed phase reactions are particularly important for core debris interactions with high silica, low gas concretes. CORCON-Mod3 assumes that chemical equilibrium is achieved between the reactants during each time step. The chemical equilibrium solver minimises the Gibbs free energy function subject to constraints on mass conservation and on non-negativity of concentrations. The condensed phase reactants and oxidic products are treated as ideal solutions. It should be noted that non-ideal solution chemistry has been included in the vapourisation release model in VANESA. CORCON-Mod3 contains coding to calculate the reduction of oxides at the pool surface by the oxygen-poor atmosphere above the melt.

7.1.2.4. Coolability models

If a coolant layer is present, CORCON-Mod3 calculates heat transfer to coolant using standard pool boiling correlations. Corrections are made for the effects of gas injection at the melt-coolant interface and coolant subcooling. Nucleate boiling is treated using the Rohsenow (Rohsenow, 1952) correlation for the temperature rise and the Zuber correlation (Zuber, 1958)(with Rohsenow’s coefficient) for the critical heat flux. The effect of subcooling on nucleate boiling is included, using the expression recommended by Ivey (Ivey, 1962). The film boiling regime is based on the Berenson correlation (Berenson, 1961) for the heat transfer coefficient in film boiling and for the temperature difference at the Leidenfrost temperature (minimum film boiling point). Above the Leidenfrost point, the total heat flux includes a radiation heat flux component and a convective heat flux component. The radiative contribution is given for infinite parallel grey walls.

CORCON-Mod3 includes the effects of gas sparging. Both gas sparging (i.e. non-condensable gas injection at the interface) and coolant subcooling can greatly increase the film boiling heat flux, while also increasing the temperature at which the vapour film collapses (the Leidenfrost point). Gas sparging increases film boiling heat transfer by increasing agitation of the coolant and of the melt surface. In CORCON-Mod3, the enhancement to the film boiling heat flux due to gas sparging is included as a multiplicative factor. The factor used depends on whether the surface underlying the coolant is solid or liquid. If the surface underlying the coolant is liquid, then the enhancement factor is calculated using a correlation of experimental results advanced by Greene (Greene, 1991). If the surface underlying the coolant is solid, then the enhancement factor is calculated using a correlation advanced by Duignan (Duignan & Greene, 1989). When the temperature of the core debris is
calculated to lie between the solidus and liquidus temperatures of the debris mixture, the two enhancement factors above are weighted by the surface solid fraction.

Subcooling of the overlying coolant pool can also enhance heat transfer in the film boiling regime. When the overlying coolant pool is subcooled, energy is removed from the gas film by the overlying subcooled coolant. The net effect of this cooling is a reduction in the thickness of the vapour film. The reduced film thickness permits greater heat transfer by conduction. The enhancement to heat transfer owing to coolant subcooling in the film boiling regime is given by a multiplicative factor, which is calculated using an equation of the form proposed by Siviour and Ede (Siviour & Ede, 1970), and Dhir and Purohit (Dhir & Purohit, 1978). The effect of coolant subcooling on the minimum film boiling temperature is calculated using a simple linear correlation of experimental data.

CORCON-Mod3 currently has no water ingress or melt eruption models. It is planned to incorporate these models from CORQUENCH (Farmer, 2010) in the future.

7.1.2.5. Melt spreading

Mechanistic modelling of the spreading process is not attempted CORCON-Mod3. Instead, CORCON-Mod3 allows the user to specify a time-dependent radius of the melt that is less than the dimension of the confining cavity. This approach allows considerable flexibility in mimicking the spreading process, while also rendering the possibility of specifying physically unreasonable melt configurations. The option, as currently implemented, will adjust the melt radius to keep the melt thickness between the maximum and minimum thicknesses specified by the user. Coolant may be present in the cavity when the time-dependent melt radius option is invoked.

7.1.2.6. Code simplifications and limitations

The CORCON-Mod3 code has some limitations which are listed below.

1) For models in dry conditions:

- The calculation of radiative heat loss from the pool surface is based on a one-dimensional model.
- The convective loss from the pool surface is calculated using a constant heat transfer coefficient.
- The calculated concrete response is based on one-dimensional steady-state ablation, with no consideration given to conduction into the concrete or to decomposition in advance of the ablation front.
- The solidification model assumes that a crust forms on any surface whose temperature falls below the solidification temperature. The mechanical stability of the crusts is not considered.
- The code assumes that the crust has the same properties as the bulk liquid phase. This may not be true if the liquid phase composition is changing with time.
- The gas-film model is used for radial heat transfer even after the melt solidifies. As a result, no radial gap develops around a layer of the melt which has completely solidified. The model assumes no radial gap around a layer of solidified melt for the purpose of heat transfer calculations though, in reality, a radial gap can form. These modelling assumptions affect the calculated shape of the cavity.
- The code assumes ideal chemistry for bulk phase chemical reactions.
- The code uses the Fe-Cr-Ni phase diagram for the metal phase that neglects important metallic components such as Zr, Si, or Al that may be present in the melt at various times during core-concrete interactions.
The time-dependent melt radius model allows the user to mimic the spreading of a melt across a horizontal surface, but it is not a mechanistic model of spreading.

2) For models in wet conditions:

- The code uses flat plate pool boiling correlations to model heat transfer to an overlying coolant pool. Generally, the code predicts lower early heat fluxes than in the experiments and long-term heat transfer by film boiling.
- The code currently has no water ingression or melt eruption models.

7.1.3. **CORIUM2D CODE**

7.1.3.1. **Pool interface models**

7.1.3.1.1. **Pool/concrete interface model**

This section is intentionally left blank.

7.1.3.1.2. **Pool upper interface model**

This section is intentionally left blank.

7.1.3.2. **Thermal-hydraulics models**

7.1.3.2.1. **Heat convection within a pool layer**

**Model assumption**

The code simulation of heat transport phenomena within a molten pool is mainly based on Fieg's (Fieg, 1978) and Kulacki-Goldstein's (Kulacki & Goldstein, 1972) experimental observations on internally heated liquids. The models proposed by these researchers give some correlations which allow determining the heat transfer from the pool to the surroundings. The pool is schematised as having three types of surfaces (top, side and bottom). A characteristic heat transfer correlation is proposed for each surface.

The Rayleigh number is defined as:

\[
Ra = \frac{Gr}{Pr}
\]

**Equation 7.1-5**

where \( Gr \) is the Grashof number:

\[
Gr = \frac{H^3 g \Delta T \beta \rho^2}{\mu^2}
\]

**Equation 7.1-6**

and \( Pr \) is the Prandtl number:

\[
Pr = \frac{c_p H}{k}
\]

**Equation 7.1-7**

where:

- \( H \) the pool height [m],
- \( g \) the gravity acceleration (= 9.81 m/s²),
- \( \Delta T \) the temperature difference between the centre and the periphery of the pool [K],
- \( \beta \) the coefficient of thermal expansion [1/K],
the density of the molten material [kg/m³],

μ the dynamic viscosity [Pa·s],

\( c_p \) the isobaric specific heat [J/kg/K] and

\( k \) the conductivity [W/m/K].

As a matter of fact, the modified Rayleigh number, \( Ra_{int} \), introduced by Fieg and commonly used for internally-heated liquid pools in most literature, represents a simpler way to calculate the Rayleigh number, starting from the specific heat source instead of the actual \( \Delta T \):

\[
Ra_{int} = \frac{g \beta}{\eta \alpha k} S H^5 \tag{7.1-8}
\]

where:

\[
\eta = \frac{\mu}{\rho} \quad \text{is the kinematic viscosity [m²/s];}
\]

\[
\alpha = \frac{k}{\rho c_p} \quad \text{is the thermal diffusivity [m²/s].}
\]

However, this way to account the Rayleigh number implies two important assumptions:

- the bulk-wall \( \Delta T \) is a function of the internally generated heat flux exiting the liquid pool at thermal equilibrium;
- the bulk-wall \( \Delta T \) is due to a purely conductive heat transfer in a uniform heat-generating medium, with no account for any liquid convection.

With these restrictions, \( \Delta T \) can be approximated as:

\[
\Delta T = \frac{H^2 S}{k} \tag{7.1-9}
\]

While this way to account \( \Delta T \) could be a reasonable approximation in the case of a lab-scale enclosure at low specific heat sources, it evidently leads to overestimate the Rayleigh number if the liquid convection is effective. In the case of corium pools at reactor scale, unreasonable liquid bulk temperatures would result. Thus, the code uses a realistic way to calculate the Rayleigh number for internally-heated liquid pools, taking into account the actual bulk-wall \( \Delta T \).

In a stationary regime, it is:

\[
\frac{Q}{A} \approx S \cdot \frac{H}{2} \tag{7.1-10}
\]

where \( S \) is the volumetric heat source [W/m³]. On the other hand, the Nusselt number is defined by means of the following relation:

\[
\frac{Q}{A} = \frac{k}{H} Nu \Delta T \tag{7.1-11}
\]

Merging the two last equations, we have:
\[ \Delta T = \frac{Q \cdot H}{A \cdot k \cdot Nu} = \frac{S}{2} \cdot \frac{H}{k \cdot Nu} = \frac{S}{2} \cdot \frac{H^2}{k \cdot Nu} \]  
Equation 7.1-12

Obviously, the value of \( \Delta T \) is limited by the difference between the boiling and melting temperatures of corium, i.e.

\[ \Delta T_{\text{max}} = T_{\text{boil}} - T_{\text{melt}} \]  
Equation 7.1-13

The Rayleigh number can now be evaluated as:

\[
Ra = \begin{cases} 
\frac{SH^2 g \beta \rho}{2k \mu^2} \cdot \frac{Pr}{Nu} & \text{if } \frac{SH^2}{2kNu} < T_{\text{boil}} - T_{\text{melt}} \\
H^2 g \beta \rho \frac{Pr}{\mu^2} \cdot (T_{\text{boil}} - T_{\text{melt}}) & \text{if } \frac{SH^2}{2kNu} \geq T_{\text{boil}} - T_{\text{melt}}
\end{cases}
\]

Equation 7.1-12

**Figure 7.1-3:** Value of internal Rayleigh number versus pool Height

The code derives the Nusselt numbers for the boundary surfaces, according to the correlations deduced by Fieg for a heated pool having aspect ratio of \( H/D = 0.25 \), and within the range \( 10^7 < Ra < 4 \times 10^9 \):

\[ Nu = \begin{cases} 
0.414 \cdot Ra^{0.216} & \text{for upward facing surfaces} \\
1.120 \cdot Ra^{0.103} & \text{for downward facing surfaces} \\
0.163 \cdot Ra^{0.244} & \text{for lateral surfaces}
\end{cases} \]

Equation 7.1-13

CORIUM-2D does not manage the liquid pool as a unique volume, with a bulk temperature and a pool depth. In fact, because of the need of tracking the corium solidification, the cell nodalisation is maintained in the molten pool, and the following strategy is adopted.

Taking a geometric mean of correlations of the last equation with a double weight for the lateral surface, we can write:

\[ \overline{Nu} = 0.333 Ra^{0.202} \]  
Equation 7.1-14
which, when substituted in Equation 7.1-14 allows estimating the Rayleigh number $Ra$. If $S$ and, hence, $Ra$ happen to be zero, $Nu=1$, i.e. pure conduction, is assumed.

Then, for each cell of the molten pool, separate Nusselt numbers for the flows in the horizontal and vertical directions are computed. While the corresponding term of Equation 7.1-15 is used in the former case, an interpolation between the correlations Equation 7.1-15 for the upward and downward facing surfaces is assumed in the latter case with the relative weights of the two terms depending on the position of the cell in the molten pool. The above procedure, implemented in CORIUM-2D code at RSE, obviously leads to Rayleigh numbers much lower than those predictable with the usual correlation for internal Rayleigh (see Figure 7.1-3). Moreover, with the physical restriction imposed by the maximum $\Delta T_{\text{max}} = T_{\text{boil}} - T_{\text{melt}}$, it turns out that the maximum Rayleigh number reachable in a reactor situation is of the order of:

$$Ra_{\text{max}} \approx 10^{12} \cdot H^3 \quad \text{Equation 7.1-15}$$

**Equivalent conductivity at steady-state regime**: While heat transfer through a liquid-solid interface is traditionally calculated accounting pure heat conduction through the related boundary layer, within the pool bulk also the contribution of the inter-cell mass transfer must be taken into account, as follows:

$$\frac{Q}{A} = k \frac{\Delta T}{\Delta x} + \frac{\Gamma c_p \Delta T}{A} \quad \text{Equation 7.1-16}$$

where:

$\Delta x = \text{distance between two adjacent cells exchanging heat [m]}$;

$\Gamma = \text{mass flow between cells [kg/s]}$;

$A = \text{flow area [m}^2\text{]}$.

The mass flow $\Gamma$ can be computed as

$$\Gamma = u' \rho A \quad \text{Equation 7.1-17}$$

$u'$ being the velocity of the mass transferred between cells, assumed to be the turbulent component of the flow regime.

Now, three simplifying fundamental hypothesis are adopted to account for the convective heat transfer.

1. It is assumed that the characteristic fluid velocity distribution (and then the related Nusselt numbers) generated by corium natural convection is equivalent to the distribution which could be obtained under forced convection, so that:

$$Nu_{\text{nat}} = Nu_{\text{forced}} \quad \text{Equation 7.1-18}$$

$Nu_{\text{nat}}$ and $Nu_{\text{forced}}$ being respectively the Nusselt numbers of the natural and equivalent forced convections.

This hypothesis allows estimating a characteristic velocity of the molten corium, associated to each cell. In fact, a relation between this velocity and the Nusselt number can be obtained through the Dittus-Boelter forced correlation for a cooled liquid (taking an average of Nusselt numbers, depending on the cell location within the pool):
where \( u_\infty \) is the characteristic velocity [m/s] of the molten corium at steady state.

2. Within a generic cell, the fluid moves in all directions with its characteristic velocity (distinguishing only between horizontal and vertical motions, but not between up and down, and right and left).

3. The velocity of mass transfer between cells may be thought as proportional to the actual velocity of the fluid within each cell which may be computed from the characteristic velocity \( u_\infty \) applying a correction factor \( f_\tau \) for mechanical inertia of the corium (see next paragraph), i.e.

\[
u' = \chi u = \chi f_\tau u_\infty \quad \text{Equation 7.1-20}
\]

where \( \chi \) is the proportionality constant which may be set by the user (a default value of 0.02 is provided by the code).

Under these hypotheses, Equation 7.1-18 becomes:

\[
\frac{Q}{A} = \frac{k}{\Delta x} \Delta T + \chi u \rho c_p \Delta T \quad \text{Equation 7.1-21}
\]

which, substituting the value of \( u \) given by Equation 7.1-21 leads to:

\[
\frac{Q}{A} = \frac{k}{\Delta x} \Delta T + \left( 111.65 f_\tau \frac{\mu}{H} Pr^{-0.375} Nu^{1.25} c_p \right) \Delta T \quad \text{Equation 7.1-22}
\]

It is possible to define an equivalent conductivity taking into account both pure conductive and convective heat transfer, i.e.

\[
\frac{Q}{A} = \frac{\Delta T}{\Delta x} k_{eq} \quad \text{Equation 7.1-23}
\]

where:

\[
k_{eq} = k + 111.65 \frac{\Delta x}{H} f_\tau \chi \mu Pr^{-0.375} Nu^{1.25} c_p \quad \text{Equation 7.1-24}
\]

Because of the uncertainties of this kind of arguments, the option of arbitrarily changing the proportionality constant between the turbulent and the characteristic velocity of the fluid within each cell of the molten pool is provided in CORIUM-2D code by means of an input assignment. This allows evaluating the importance of the convective motions inside the pool for the time evolution of the system.

7.1.3.2.2. Heat convection between oxide and metal layers in a stratified pool

A pure conduction heat transfer model is considered at oxide/metal interface.

7.1.3.2.3. Stratification criteria

Input defined
7.1.3.2.4. **Pool/concrete interface model**

The heat transfer at pool/concrete interface is described by a Fieg’s modified model and Kulacki-Goldstein’s correlation modified for non-stagnant pool. A stable crust is assumed for the pool/concrete interface structure.

7.1.3.2.5. **Pool upper interface model**

The heat transfer at pool upper interface is described by a Fieg’s modified model and Kulacki-Goldstein’s correlation modified for non-stagnant pool.

7.1.3.3. **Thermophysical properties**

CORIUM-2D materials may be thought as an aggregation of different components, which are uranium and plutonium (both oxide and metal), zirconium oxide, Zircaloy, carbon steel, water, stainless steel, concrete, sodium, lead and lead-bismuth eutectic. For each material, a library of constant properties (melting and boiling temperature, latent heat of fusion, and emissivity) and of temperature-dependent properties (density, specific heat, thermal conductivity, dynamic viscosity, isobaric expansion coefficient) is included in the code. Dependence on pressure is also taken into account for water. The properties of each component are computed by means of empirical correlations, while the properties of the composite materials are evaluated weighting the properties of each component with the corresponding molar or mass fraction. In particular, each component may undergo a phase change independently of the others, since solidus and liquidus temperatures are defined for each component. In any case, thermal properties are evaluated for every component by means of the corresponding correlations or solidus-liquidus interpolations and finally averaged.

7.1.3.4. **Coolability models**

This section is intentionally left blank.

7.1.3.5. **Code simplifications and limitations**

This section is intentionally left blank.

7.1.4. **CORQUENCH code**

7.1.4.1. **Pool interface models**

7.1.4.1.1. **Pool/concrete interface model**

The pool-concrete interface can be modelled in several ways, depending upon user input options. In particular, the interface can be modelled as devoid of crust material, or transient crust growth, stabilisation, re-melting, and subsequent failure can be modelled. For situations in which the concrete surface is modelled as crust-free, the heat transfer coefficient from the melt pool to the melt/concrete interface can be selected from i) a slag film model (Bradley, 1988) (viz. Bradley’s modification to the bubble agitation heat transfer model of Kutateladze and Malenkov (Kutateladze & Malenkov, 1978), ii) gas film models similar to those deployed in CORCON Mod3 (Bradley, Gardner, Brockmann, & Griffith, 1993), or iii) the empirical correlations developed by Sevón (Sevón, 2008) on the basis of the CCI test results. When crust growth is modelled, heat transfer from the pool to the crust is modelled using the Kutateladze and Malenkov correlation (Kutateladze & Malenkov, 1978). Crust failure can be specified as occurring at a minimum thickness, or failure can occur based on a simple mechanical loading model that is orientation-dependent.

7.1.4.1.2. **Pool upper interface model**

Depending upon the pool thermal-hydraulic conditions, the upper surface can be devoid of a stable crust, or transient crust growth is calculated by solving a growth rate equation; the crust material composition is treated separately from the melt material composition. When water is present, a
detailed heat transfer analysis is performed at the crust-water interface involving a variety of potential core debris cooling mechanisms; see 7.1.4.5. On the crust-pool side of the interface, the convective heat transfer coefficient is calculated using the correlation of Kutateladze and Malenkov (Kutateladze & Malenkov, 1978).

7.1.4.2. Thermalhydraulics models

7.1.4.2.1. Heat convection within a pool layer

A detailed fluid mechanics calculation of convection within the pool is not calculated. Rather, the pool is treated as well-mixed and at a uniform bulk temperature. Heat transfer at the pool boundaries is modelled using convective heat transfer correlations. The code can treat fully oxidic to fully metallic melt compositions, or both phases can be present. However, the code assumes that the oxide and metal phases are well mixed at all times; i.e. phase stratification is not modelled.

7.1.4.2.2. Heat convection between oxide and metal layers in a stratified pool

As noted in section 3.2.4, CORQUENCH treats oxide and metal phases as well-mixed. Stratification is not modelled.

7.1.4.2.3. Stratification criteria

Stratification is not modelled. This assumption is not valid for melt pools containing a significant metal content under low gas sparging scenarios that can be encountered, for example, with siliceous concrete.

7.1.4.3. Thermophysical properties

The MCCI conservation of mass equations and thermophysical property subroutines consider most core and concrete metals and their corresponding oxides. Melt viscosity is calculated using the Andrade formula (see Nazare et al. (Nazare, Ondracek, & Shulz, 1977) with a correction for SiO2 as developed by Shaw (Shaw, 1972). Viscosity enhancement due to build-up of solids within the melt can be calculated using either the Ishii-Zuber (Ishii & Zuber, 1979) or Kunitz (Kunitz, 1926) models. Debris specific enthalpy for the various zones is calculated by weighting the individual constituent enthalpies on a molar basis. Melt void fraction can be evaluated from one of several different correlations; i.e. those due to Brockmann et al. (Brockmann, Arellano, & Lucero, 1989), Wallis (Wallis, 1979), or Kataoka and Ishii (Kataoka & Ishii, 1987).

7.1.4.4. Thermo-chemical properties

CORQUENCH does not perform a detailed thermo-chemical properties evaluation; rather, the particular species of key melt constituents (e.g. UO2) is assumed. Distinct metal and oxide phases are treated as part of the evaluation; as noted previously, these phases are assumed to be mixed. The rate of oxidation of metallic melt constituents is based on the rate at which oxidising gases from concrete erosion (CO2 and H2O) are sparging through the melt. The oxide phase diagram is based on the Lambertson-Mueller phase diagram (Lambertson & Mueller, 1953) for UO2-ZrO2 with empirical corrections for concrete content based on the data obtained by (Roche, Leibowitz, & Fink, 1993). For situations in which crust is forming, the crust-melt interface temperature can be based on the crust composition or the melt composition (user-input modelling assumption).

There is no treatment of chemical reactions between the melt and the atmosphere, or of reactions in the atmosphere. Metals contained in frozen material do not undergo additional chemical reaction (unless that debris is re-melted). In reality, some additional oxidation reactions may be expected due to debris permeability and diffusion within the solidified material.

CORQUENCH does not currently evaluate fission product source term which is an important element in reactor safety evaluations.
7.1.4.5. Coolability models

Heat transfer at the pool upper surface under wet-cavity conditions has been the principal focus area for CORQUENCH development. Bulk cooling and incipient crust formation are calculated using the models developed by Farmer et al. (Farmer, Sienicki, & Spencer, 1990), (Farmer, Spencer, Schneider, Bonomo, & Theofanous, 1992). Following incipient crust formation, crust growth is calculated by solving a growth rate equation; the crust material composition is treated separately from the melt material composition. The melt-side convective heat transfer coefficient is calculated using the correlation of Kutateladze and Malenkov (Kutateladze & Malenkov, 1978). For the case in which the crust is treated as permeable to water ingestion, then the crust dry-out limit can be calculated using either a user-specified crust permeability, or the dry-out heat flux can be calculated using the Lomperski and Farmer model (Lomperski & Farmer, 2007).

For situations in which a particle bed develops over the crust, the heat flux from the crust upper surface may be limited by the particle bed dry-out limit. For this case, the bed dry-out limit is calculated with the Lipinski correlation (Lipinski, 1980). The heat flux from the crust upper surface is checked during the calculation to ensure that it does not exceed the effective dry-out limit. Particle bed formation by melt eruptions is evaluated using the approach of Bonnet and Seiler (Bonnet & Seiler, 1992); i.e. melt dispersal is calculated by assuming that the melt entrainment rate is proportional to the gas volumetric flowrate times an entrainment coefficient. Several options are provided for evaluating the melt entrainment coefficient: i) the user may specify the coefficient directly, ii) the entrainment coefficient can be evaluated with the Ricou-Spalding model (Ricou & Spalding, 1961), or the coefficient can be evaluated using the model due to Farmer (Farmer, Phenomenological Modeling of the Melt Eruption Cooling Mechanism during Molten Corium Concrete Interaction, 2006). Consistent with test observations, the dispersed melt is assumed to be rendered in the form of an accumulating particle bed (with specified particle diameter and porosity) on top of the crust.

The model can also calculate crust anchoring to the cavity sidewalls, as well as the subsequent melt/crust separation phase which arises due to concrete densification upon melting. For a given cavity span, the minimum crust thickness required to be mechanically stable due to the combined weights of the overlying water pool, particle bed, and the crust itself is evaluated using a first-order plate strength equation from Roark and Young (Roark & Young, 1975). During the calculation, the upper crust thickness is compared with that predicted from the Roark and Young equation. When the thickness exceeds the minimum required to be mechanically stable in the given cavity configuration, the crust is assumed to attach to cavity sidewalls with the upper surface elevation fixed at the location of anchoring. Thereafter, the voided melt upper surface location is tracked relative to the crust location so that the onset of gap formation can be predicted. When a gap does form, debris quenching by the mechanisms of crust water ingress and melt eruptions is terminated, and there is a corresponding reduction in upwards heat transfer due to solidification (latent heat) processes. Moreover, a heat transfer resistance across the gap is introduced into the heat balance, which causes a further reduction in upwards heat transfer. This methodology allows the prediction of the crust anchoring time and location for comparison with test results such as those obtained in the MACE Program.

7.1.4.6. Code simplifications and limitations

The main simplifications that arise from utilisation of a lumped parameter approach, simple geometry assumptions, and other CORQUENCH limitations are summarised below.

Dry cavity condition models

In terms of interaction with the cavity, the atmosphere and surroundings above the pool surface serve only to provide boundary conditions for heat and mass transfer from the pool, as the code does not include calculation procedures to update the temperature, pressure, or composition of the atmosphere or the temperature of the surroundings. The calculation of radiation heat loss from the
pool surface is based on a one-dimensional model with the boundary temperature constant at a user-specified value. No radiation attenuation by aerosols is treated.

Wet cavity condition models

CORQUENCH has fairly detailed and robust models for core debris cooling under a variety of scenarios ranging from initial bulk cooling, incipient crust formation, and further debris cooling with the possibility of treating melt eruption and/or water ingestion cooling mechanisms. Although discrete debris regions with unique compositions are treated, the compositions of these zones are treated in a bulk fashion; i.e. local composition gradients are not evaluated. The particle size and porosity of beds formed by melt eruptions are user-specified as opposed to calculated.

Reactor cavity geometry

CORQUENCH utilises a highly idealised cavity modelling approach in which the cavity is envisioned to consist of either a right cylinder or a notch-type configuration. Although lateral and axial ablation rates are calculated, one of these (user-specified) geometries is assumed and maintained over the balance of the calculation. Thus, true cavity rounding effects that are observed in experiments are not calculated with this code.

Scenario limitations

CORQUENCH does not have a dedicated spreading model; the initial cavity dimensions are specified and that geometry is assumed through the balance of the calculation. The code can accommodate ongoing melt pour conditions from the RPV. This material is assumed to relocate to the melt zone of the core-concrete interaction; i.e. no interaction with crust and/or particle bed atop the melt zone is treated. In reality, spreading over this previously solidified material would most likely occur. In addition, melt fragmentation and cooling during relocation through water over the pool is not addressed in the code.

7.1.5.  **COSACO code**

7.1.5.1.  **Pool interface models**

7.1.5.1.1.  **Pool/concrete interface model**

In COSACO, the heat transfer coefficient from the oxidic melt pool is calculated using a correlation based on the BALI tests (Bonnet, 1999). The boundary temperature used together with this heat transfer coefficient is equal to the concrete decomposition temperature. This decomposition temperature is defined for most COSACO analyses as the temperature for which a solid volume fraction of the concrete of 50% is reached.

A quasi-steady concrete erosion without considering thermal conduction in the concrete is used. The locally eroded concrete thickness increment is determined by dividing the heat transferred from the melt during a time-step by the concrete decomposition enthalpy.

For oxide pool temperatures below the liquidus temperature, phase segregation with crusts at the pool boundaries is considered. The total amount of solid material in the pool is predicted by the thermo-chemical solver and is then split between the pool bulk (as floating crystals) and the pool boundaries (as a crust). The proportion of the total solid settling on a given boundary is derived from the relative importance of the heat sink associated to this boundary. On the basis of this locally deposited amount and the surface area of the boundary, a crust thickness is derived. An effective heat transfer from the melt pool to the concrete is determined by a series connection of the resistances from the pool and the crust. Using the same assumptions, the crust and the heat transfer coefficient at the oxide/metal interface for layered melt configurations is determined. The effective heat transfer
coefficient is determined by the individual thermal resistances in the layers (connection in series),
including that of the crust, if any.

Unlike oxidic corium, the metallic melt has a high thermal conductivity and is less prone to the
formation of insulating crusts at the concrete surface. Therefore, no presence of crust is taken into
account for the metallic melt layer. The interface temperature is chosen as the interface temperature
between two semi-infinite solid slabs brought into contact. Since, in addition to the heat conduction,
also convection occurs in the metallic pool, the effective conductivity used for the calculation of the
contact temperature is empirically determined to be around ten times the molecular value. The heat
transfer coefficient from the metallic pool to the concrete is determined using the Kutateladze-
Malenkov correlation (Kutateladze & Malenkov, 1978).

7.1.5.1.2. Pool upper interface model

In COSACO, radiation heat transfer from the melt pool top surface is modelled. For configurations
where the top melt layer includes refractory oxides, a top crust is assumed and considered in the
effective heat transfer coefficient. This radiation yields also concrete erosion in the upper lateral cavity
walls. For situation where the melt pool is flooded with water, a simplified boiling heat transfer
correlation is included.

7.1.5.2. Thermalhydraulics models

7.1.5.2.1. Heat convection within a pool layer

The convection in an oxidic or mixed melt layer is determined by using a correlation derived from the
BALI experiments (Bonnet, 1999). Due to the intense mixing of the melt pool by the MCCI gases, the
same heat transfer mechanism in radial direction as downwards is assumed. Therefore, the same heat
transfer correlations are also assumed.

For metal melt layers, the Kutateladze-Malenkov correlation (Kutateladze & Malenkov, 1978) is
used.

7.1.5.2.2. Heat convection between oxide and metal layers in a stratified pool

In layered melt configuration, the BALI and the Kutateladze correlations are used for the oxide and
metal pool layer, respectively. A crust of refractory solid oxides can form at the interface. The overall
heat transfer coefficient is determined by means of a series connection of the different heat resistances.

7.1.5.2.3. Stratification criteria

The melt pool configuration (mixed or layered) is preset by user input. In layered melt configurations,
initially three layers with oxidic melt, metallic melt and slag are considered (from bottom to top). With
ongoing concrete erosion and addition of light concrete oxides, the oxide melt density decreases. This
yields a configuration with the oxidic melt layer (including also the slag) above the metal melt.

7.1.5.3. Thermophysical properties

During the calculation, the melt state is determined by means of a thermo-chemical equilibrium solver,
The solid and liquid fractions of the melt components are retrieved from the obtained equilibrium
state. The thermo-physical properties of the melt (in mixed and layered configurations) are then
determined by the use of mixture laws. The viscosity of the melt layers is calculated using the model
of Urbain (Urbain, 1987). The influence of solidified fractions on the overall melt viscosity is
considered using a correlation suggested by Ramaciotti (Ramacciotti, Journeau, Sudreau, & Cognet,
2001).
7.1.5.4. *Thermo-chemical properties*

In COSACO, the thermo-chemical properties of the melt layers are based on the real solution database COSCHEM, a subset of the TDBCR99 database (Chevalier, 1999). The thermo-chemical equilibrium is calculated within each time step using the ChemApp solver (CHEMAPP, 1999).

7.1.5.5. *Coolability models*

In COSACO, the top melt surface temperature is calculated. In dry conditions, radiation from this surface is considered. Furthermore, flooding of the melt pool can be used. A simplified boiling curve is included for this case.

No dedicated modelling on water ingestion, melt eruption and debris cooling is currently included. It is foreseen to implement these features.

7.1.5.6. *Code simplifications and limitations*

This section is intentionally left blank.

7.1.6. *MAAP CODE*

7.1.6.1. *Pool interface models*

7.1.6.1.1. *Pool/concrete interface model*

Crust is always assumed to be present at the interface between the corium and concrete. When the corium is first relocated (discharged) from the failed reactor vessel, a minimum crust thickness of 1 mm is assumed. The crust is assumed to be quite permeable to the concrete slag and gases. The molten slag and gas can enter the corium pool immediately, and the interface between the crust and the concrete does not have an accumulated slag or gas layer. The contact (thermal) resistance between the crust and the concrete is also ignored. Therefore, the temperature of the outer surface of the crust is identical to the temperature at the surface of the concrete.

The temperature profile in the crust is assumed to follow a parabolic distribution, as shown in Figure 7.1-4. The inner surface temperature of the crust is equal to the molten (solidus) temperature $T_{F,m}$ of the melt. The outer surface temperature of the crust is equal to the concrete surface temperature $T_i$. Therefore, the temperature profile $T(x)$ is given by

$$T(x) = T_i + (T_{F,m} - T_i) \left(1 - \left(\frac{x}{x_c}\right)^2\right)$$  \hspace{1cm} \text{Equation 7.1-25}

where $x$ is the distance in the crust measured from the inner surface and $x_c$ is the thickness of the crust. The growth (or shrinkage) rate of the crust is based on the overall energy balance in the crust. This can be represented as

$$\frac{dx_c}{dt} = \frac{q_{\text{conv}} + q_{\text{v'}} x_c - q_{\text{cond}}}{\rho_c L H_c}$$  \hspace{1cm} \text{Equation 7.1-26}

where $q_{\text{conv}}$ is the convective heat flux from the molten corium pool to the crust, $q_{\text{v'}}$ is the volumetric decay power generation rate in the crust, the $q_{\text{cond}}$ term represents the conduction heat flux from the crust to the concrete, $\rho_c$ is the density of the crust, and $L H_c$ is the latent heat for the crust.
Once the heat flux from the crust to the concrete is determined, the response of the concrete to the imposed heat flux is determined by balancing energy at the ablation front. The concrete is modelled as a one-dimensional slab, nodalised in the direction of heat transfer. Rates of temperature changes in the slab are solved through the one-dimensional heat conduction equation. For a given heat flux $q_{\text{cond}}$, the ablation rate is calculated through the energy balance at the interface as

$$\frac{dx_{\text{cn}}}{dt} = \left( q_{\text{cond}} - q_{\text{cond, cn}} \right)/\left( \rho_{\text{cn}} \cdot L_{H_{\text{cn}}} \right) \text{ Equation 7.1-27}$$

where $x_{\text{cn}}$ is the thickness of the concrete slab, $q_{\text{cond, cn}} = -k_{\text{cn}} \frac{dT_{\text{cn}}}{dx}|_{x=x_n}$ is the heat flux conducted away from the front, $\rho_{\text{cn}}$ is the concrete density. The latent heat term $L_{H_{\text{cn}}}$ is a lumped latent heat of concrete including the latent heat for both melting and decomposition processes.

### 7.1.6.1.2. Pool upper interface model

Unlike the corium pool/concrete interface, crust may or may not be formed at the upper interface. If the cavity is dry, the temperature at the upper interface may exceed the melting (solidus) temperature of the corium pool, and no crust is formed for this case.

If the crust exists at the upper interface, the temperature profile in the crust is assumed to follow a parabolic distribution similar to the bottom and side crusts. The temperature at the upper surface of the crust is determined by the energy balance between the heat loss through radiation (if the cavity is dry) or convection (if the cavity is flooded) and conduction heat transfer through the crust. If the crust does not exist, the upper surface temperature is determined by the energy balance between the upward heat loss and convective heat transfer from the molten pool centre to the upper surface. The energy loss from the upper crust to water is determined by the corium coolability model in Section 7.1.6.5.

### 7.1.6.2. Thermalhydraulics models

#### 7.1.6.2.1. Heat convection within a pool layer

The convective heat transfer is modelled in MAAP5.01 via two options. The first option relies on users to provide the convective heat transfer coefficients for downward, sideward, and upward convective heat transfer from the molten pool centre to the crusts. The heat fluxes are given by

$$q = \begin{cases} 
  h_d (1-f_s)^{\gamma} \left( T_p - T_{F,m} \right) & \text{for bottom crust} \\
  h_s (1-f_s)^{\gamma} \left( T_p - T_{F,m} \right) & \text{for side crust} \\
  h_u (1-f_s)^{\gamma} \left( T_p - T_{F,m} \right) & \text{for upper crust} 
\end{cases} \text{ Equation 7.1-28}$$

where $h_d$, $h_s$, and $h_u$ are the respective heat transfer coefficients, $T_F$ is the average temperature of the pool, and $T_{F,m}$ is the crust melting temperature or surface temperature (if upper crust does not exist). The centre of the pool is modelled as fully or partially molten. If the centre is partially molten, the
effect of viscosity is represented by the term \((1-f_s)^n\), where \(f_s\) is the solid fraction in the molten centre. Exponent \(n\) is a user provided parameter to account for this effect.

Guidance has been provided in the MAAP manual to help users choose the values of the heat transfer coefficients and the exponent. Default values of these parameters are \(h_d=3\,500\) W m\(^{-2}\)K\(^{-1}\), \(h_s=h_u=3\,000\) W m\(^{-2}\)K\(^{-1}\) and \(n=2.75\). These values are from comparisons between MAAP predictions and dry cavity MCCI experiments. The second option is used if users decide not to provide the values of the coefficients. MAAP code will calculate the coefficients based on the correlations suggested by Mayinger et al. (Mayinger, Jahn, Reineke, & Steinbrenner, 1976). for this case:

\[
\begin{align*}
N_u &= 0.36 \, Ra^{0.23} \quad \text{Equation 7.1-29} \\
N_d &= 0.54 \, Ra^{0.18}(H/R)^{0.26} \quad \text{Equation 7.1-30} \\
N_s &= (N_u + N_d)/2 \quad \text{Equation 7.1-31}
\end{align*}
\]

where \(N_u\), \(N_d\), and \(N_s\) are respectively the Nusselt numbers for upward, downward, and sideward heat transfer, \(Ra\) is the Rayleigh number, \(H\) is the depth of the pool and \(R\) is the radius of the upper surface of the pool. This correlation was actually based on an experiment to simulate an in-vessel corium pool. The pool is assumed to be surrounded by crusts and turbulent natural convection is developed in the molten pool.

7.1.6.2.3. Stratification criteria

No stratification is currently considered in MAAP. If a stratification model is implemented in the future, stratification criteria will be considered at that time.

7.1.6.3. Thermo-physical properties

The thermo-physical property model in MAAP is based on the phase diagram method. This method assumes that all constituents in the corium can be partitioned into metal and oxide phases. For metal, a pseudo-binary phase diagram is constructed for steel versus other metals. For oxide, a phase diagram is constructed for U-Zr-O versus other oxides. The mass of the constituents and total energy are tracked at each time step in MAAP. The phase diagrams are used to find the temperature, solid fraction and other properties that balance the mass and energy.

MAAP uses a formulation analogous to the Ishii-Zuber (Ishii & Zuber, 1979) correlation to account for the viscosity effect. As discussed in Section 7.1.6.2.1, a term \((1-f_s)n\) is applied to the convection heat flux, but the exponent is user-specified.

7.1.6.4. Thermo-chemical properties

Chemical reactions during MCCI are simulated in MAAP with a model called METOXA. This model assumes chemical equilibria at each time step. Temperature-dependent equilibrium constants and activity coefficients are used to find the equilibrium chemical compositions at a specific time. Chemical reaction heat is deduced from two consecutive time step compositions. Volatile fission product compounds formed in reactions are assumed to be carried by gas from concrete ablation, and released to the containment gas space.
Two sets of data were used by the METOXA model: Gibbs free energy data and reaction enthalpy data. The former was mainly from the NRC code VANESA (Powers, Brockmann, & Shiver, 1986). For those species and compounds not covered in VANESA, data from other sources were used (Jackson, 1971), Pankrantz (Pankrantz, 1982), Lange’s Handbook (Deans, 1985), CRC Handbook (Weast, 1983). The enthalpy data are mostly from Jackson and Pankrantz.

7.1.6.5. Coolability models

Corium coolability is modelled using two options. The first option, which is consistent with the model in all versions prior to MAAP5.01, uses input parameters to account for uncertainties in debris coolability. The second option is a mechanistic model added in MAAP5.01 to address the phenomenon of water ingestion into the upper crust.

The water ingestion phenomenon is modelled in MAAP5.01 in the first option as in earlier versions of MAAP, which use a parameter FCHF to represent the heat flux from the corium pool to water. This option assumes the heat flux from the pool is controlled by hydrodynamic stability at the top interface. The formulation of the heat flux is analogous to the critical heat flux for a flat plate suggested by Kutateladze (Kutateladze S. , 1951):

$$q^* = FCHF \left( \frac{\rho_g (\rho_l - \rho_g)}{\rho_g^2} \right)^{1/4} \rho_g h_{fg} \quad \text{Equation 7.1-32}$$

where $\rho_l$ and $\rho_g$ are respectively the densities of water and steam, $h_{fg}$ is the latent heat of evaporation, and $\sigma$ is the surface tension. Parameter FCHF is provided through user input.

The second option is a mechanistic model which does not rely on a user-provided FCHF. This model combines the studies by Lister (Listner, 1974) [on cracking and water penetration into hot solid rock, and a one-dimensional heat transfer model by Epstein (Epstein, 2006). The crust is represented by three regions in the model: at the top a quenched zone where the crust temperature is close to the water saturation temperature; in the middle a dry-out zone where the cracks are filled by superheated steam, at the bottom is a crack-free zone where the solidified crust is contiguous. A constant quenching speed is assumed in the model which allows the three regions to move downward at the same speed $u$. Decay heat generation in the crust is considered in this model.

One of uncertainties in water ingestion modelling is the role of metallic materials in the upper crust. All the experiments on heat transfer from corium to water so far were performed with oxidic materials. It is uncertain whether water ingestion will still occur and what will be the magnitude of the heat flux to water if the corium has a significant amount of metallic materials in it. The mechanistic model in MAAP has shown that permeability and heat flux to water are sensitive to properties, especially the thermal conductivity in the upper crust, which is a function of the amount of metallic material present. To allow sensitivity studies, an option has been added in MAAP5 to allow users to specify whether the thermal conductivity in the crust is calculated with all the constituents (oxidic and metallic) or with only the oxidic constituents. The default option is to calculate the thermal conductivity with only the oxidic constituents.

MAAP also has a melt eruption model, which is based on the Ricou-Spalding entrainment correlation (Ricou & Spalding, 1961), given by

$$j_m = E_0 \left( \frac{\rho_g}{\rho_m} \right)^{1/2} j_g \quad \text{Equation 7.1-33}$$

where $E_0$ is a constant, $j_m$ and $j_g$ are respectively the superficial velocities of entrained melt and gas, and $\rho_m$ and $\rho_g$ are the respective densities of the melt and gas. According to Epstein (Epstein, 2006), the value of $E_0$ should be 0.08 which is consistent with the entrainment data reported elsewhere (Tourniaire B. , Seiler, Bonnet, & Amblard, 2006) for the thin crust case.
The corium structure model in MAAP5 is a three-layer model, including a particle bed, an upper crust and the remaining continuous pool. Mass and energy in each region are integrated separately. The particle bed can be generated at the time of corium relocation by means of corium-water interaction. Users can control (through options and modelling parameters) the amount of particles generated due to the interaction and the amount deposited on top of the upper crust as a particle bed. The average size of the particles during the corium-water interaction can be either a user-input or code-calculated value. The particle bed can also be generated at the time of melt eruption. In this case, the erupted corium mass is deposited on top of the upper crust as a particle bed. The average particle size generated by melt eruption is a user input. Heat transfer from the particle bed to water is based on a correlation suggested by Lipinski (Lipinski, 1980) or the one suggested by Henry et al. (Henry, Epstein, & Fauske, 1982). The user may select either of these two models. Heat transfer from a dry particle bed to surroundings is based on the model developed by Epstein (Epstein, Cheung, Chawla, & Hause, 1981).

Water ingression is assumed to occur in the upper crust. If the heat flux from the particle bed to the overlying pool of water is less than the critical heat flux, water is available to the upper crust and ingression heat flux is evaluated. Growth or shrinkage of the upper crust is based on a heat balance from the upper crust to water and from the molten corium below to the upper crust.

The corium below the upper crust is modelled as a contiguous pool with a molten centre and crusts separating the molten centre and concrete. It does not contact water directly. Therefore there is no direct heat transfer from this region to the water pool above.

### 7.1.6.5.1. Pool shape model

The corium pool in MAAP5 can be modelled as different shapes. The default option is to model the pool as a cylinder. As ablation occurs in the floor and sidewall, the cylinder floor and sidewall move but the cylindrical shape is maintained. The second option is to model the pool as a rectangle or square with vertical sidewalls. Similar to the default option, if ablation occurs, the floor and sidewall boundaries move but the shape is maintained. For this option, users can also specify whether all four sidewalls ablate or only two of the sidewalls ablate. The third option is a sophisticated ablation shape evolvement model added in MAAP5.03. This model assumes the initial cavity shape is cylindrical. As ablation starts, different points along the floor and sidewall can move at different speeds. Eventually, the ablation front evolves from a cylinder to an axisymmetric curved shape. The last option is to model both a cavity and a sump. The cavity and sump can be cylindrical or rectangular. As ablation starts, the floors and sidewalls of the cavity and sump can move at potentially different speeds, but the cylindrical or rectangular shape of the cavity and sump is maintained.

### 7.1.6.6. Code simplifications and limitations

The simplifications and limitations in MAAP are summarised below:

**Thermo-physical property model:**

The thermo-physical property model in MAAP is based on an approach of partitioning different constituents in the corium pool into metal and oxide phases. This approach has a limitation in some cases when the partitioning between metal and oxide phases is not straightforward. For example, if a significant amount of uranium and zirconium mass is in the corium, some amount of the uranium and zirconium will be associated with the oxide U-Zr-O, while the remaining mass will be associated with steel and B_4C. The partitioning rule should ideally be based on more sophisticated multicomponent phase diagram for this case.

**Coolability model:**

One of limitations of the MAAP coolability model is the lack of a bubbling heat transfer enhancement during bulk cooling. The surface area for heat transfer to water is simply the top
area (at a single elevation) based on volume versus height table for the cavity. If a gas bubbling effect was considered, the heat transfer area would be larger than the top area based on the volume versus height table. Also, the criterion of incipient top crust formation is based strictly on the top surface temperature in MAAP5. If the temperature is less than the melting temperature of corium, a crust is assumed to form. Again, if a gas bubbling effect were to be considered, the incipient crust formation would be delayed. This limitation tends to produce conservative (perhaps overly conservative) results when corium interacting with high gas content concrete is flooded by water.

Pool shape model:

The corium pool in MAAP is assumed to be an axisymmetric shape. Non-axisymmetric shapes are not currently allowed. Also, the sophisticated ablation shape model in MAAP5.03 is currently available only for the (initially) cylindrical cavity.

It is worth noting that the limitations 1) do not significantly affect MAAP calculations of corium temperature and ablation depth. The reason for this is that the heat transfer from molten corium to the surrounding crust is based on a parametric approach in MAAP. This approach relies on heat transfer coefficients $h_d$, $h_s$, and $h_u$ and an exponent $n$ provided through inputs. The suggested values of these coefficients and exponent are based on extensive comparisons with dry and wet cavity MCCI experiments. Therefore, even if there is some error in the thermo-physical properties, the experiment-based coefficients and exponent will compensate for the error.

7.1.7. MEDICIS CODE

7.1.7.1. Pool interface models

7.1.7.1.1. Pool/concrete interface model

Two types of pool/concrete interface models can be handled by MEDICIS

**Model 1**: the heat transfer towards the pool/concrete interface is described the concept of a solidification temperature $T_{\text{solidif}}$ determining the interface temperature between the convective bulk pool zone and a mushy crust as indicated in Figure 7.1-5. In the frame of this model, the heat flux to the interface $\phi$ is equal to $h_{\text{conv}} (T_{\text{bulk}} - T_{\text{solidif}})$ and depending on the solidification temperature as long as a crust is present at the interface.

![Figure 7.1-5: Pool/concrete interface structure in case of model 1](image)

**Model 2**: this very simple model permits to account for the 2D ablation anisotropy, assuming that no stable crust can build along the pool/concrete interface whatever the concrete type; the heat transfer from the bulk pool to the concrete interface is determined only by convection in the bulk pool and heat transfer at the interface; the interface thermal resistance depending on the interface orientation is imposed using the $h_{\text{slag}}$ heat transfer coefficient, as indicated in Figure 7.1-6; this permits to model a simple slag layer made of concrete oxides or a solid accumulation or an inert crust without any link with a solidification temperature.
Model 2*: In case that the convective heat transfer coefficient (htc) turns out to be much larger than hslag as imposed by the user (an observation made in validation calculations for typical experiments) an alternative to model 2 is discussed, which introduces an effective heat transfer coefficient in replacement of the combination of slag layer and convective htc.

7.1.7.1.2. Pool upper interface model

The concept of solidification temperature is used to evaluate the interface temperature between the bulk pool and the upper crust at the upper pool interface in case of both pool/concrete interface models. The solidification temperature is evaluated either from liquidus and solidus temperature using the formula $T_{solid} = \gamma T_{solidus} + (1-\gamma) T_{liquidus}$ – the drawback of this criterion is its lack of physical basis. Therefore a new more physical criterion is used for evaluating the solidification temperature from a volumetric liquid fraction obtained by an interface with the NUCLEA thermochemistry database (Cheynet, Chaud, Chevalier, Mason, & Mignanelli, 2004) and corresponding to a corium mobility threshold of 0.5 (Cranga, Mun, & Marchetto, 2010).

**Recommendations:**

- Use of the model 2 without crust at the pool/concrete interface with a profile of hslag value in the range of a few tens to a few hundred of W/m²/K depending on the concrete type.
- Evaluation of the temperature for the upper crust build-up (solidification temperature) from a corium volumetric molten fraction equal to 0.5.
- If a crust forms heat transfer to the top surface is governed by 2 parameters: the heat transfer coefficient for melt convection and the solidification temperature.
- Other combinations are still under discussion (based on the pool/concrete interface model 2*).

7.1.7.2. Thermal-hydraulics models

7.1.7.2.1. Heat convection within a pool layer

The only mechanism described is convection due to gas bubbling. The correlation used is derived from BALI data (Bonnet, 2000). Per default heat convection is assumed to be non-depend on the interface orientation angle; as a user’s option a multiplicative factor function of angle can be used.

The recommended choice for heat convection correlation and 2D distribution (if using pool/concrete interface models 1 or 2) is the BALI based correlation with no dependence on the angle.

7.1.7.2.2. Heat convection between oxide and metal layers in a stratified pool

The correlation used for convective heat transfer is Greene’s correlation (Greene & Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988) with a possible multiplicative factor. The impact of possible crusts built-up at the oxide/metal interface on heat transfer is taken into account: heat transfer between two adjacent layers is described by a thermal resistance model taking into account heat convection within each layer on both sides of the
interface and heat conduction in the crust possibly appearing in each layer at this interface (in particular in more refractory oxide layer).

The recommendation on correlation choices and assumptions is standard Greene’s correlation with a possible oxidic crust.

7.1.7.2.3. Stratification criteria

The stratification criterion depending on gas bubbling is derived from BALISE experiments (Tourniaire, Seiler et Bonnet 2003) (Tourniaire, Seiler, & Bonnet, 2003). The superficial gas velocity Jg threshold value below which pool stratification occurs is Jg < b_HS (\(\rho_{\text{bot}} - \rho_{\text{top}}\) / \(\rho_{\text{bot}}\) m/s. The superficial gas velocity Jg threshold value above which pool stratification disappears if Jg > b_SH (\(\rho_{\text{bot}} - \rho_{\text{top}}\) / \(\rho_{\text{bot}}\) m/s.

A minimum thickness of metal layer is required for stratification occurrence and combined with the criterion on minimum Jg.

Recommendations on parameters of stratification criteria: b_HS = 0.027; b_SH > 0.1 (to avoid oscillations between stratified and homogeneous configurations); the minimum metal layer thickness required for stratification is 3 cm.

7.1.7.2.4. Thermophysical properties

Urbain’s model (Urbain, Bottinga, & Richet, 1982) is used for determining the liquid oxide mixture viscosity in combination with several optional models for evaluating the increase of the viscosity due to the solid fraction (per default Stedman’s model (Stedman, Evans, & Woodthorpe, 1990), as an option either Thomas correlation (Thomas, 1965) or Ramacciotti’s correlation (Ramacciotti, Journeau, Sudreau, & Cognet, 2001).

A consistent set of simplified enthalpy functions is used for all species either in condensed phase or in gaseous phase permitting to take into account oxidation reactions. An additive law is used for evaluating the heat capacity of mixtures. The enthalpy evaluation for a partially liquid mixture takes into account the molten mass fraction deduced from the interface with NUCLEA described hereafter.

7.1.7.3. Thermo-chemical properties

In MEDICIS, thermo-chemical data are obtained by using the thermo-chemistry database NUCLEA (Cheynet, Chaud, Chevalier, Mason, & Mignanelli, 2004). The needed thermo-chemical data (mainly molten fraction) in the range of temperature and corium composition encountered during MCCI are generated in two steps, using an interface with the GEMINI2 thermo-chemistry equilibrium code (Cheynet, Chevalier, & Fischer, 2002) and the NUCLEA database. First a set of compositions of interest is defined: this is the line in the compositions space obtained by mixing the given initial composition mass with increasing amounts of the considered concrete, taking into account oxidation reactions between the gases released from the concrete and the corium. Then, in a pre-processing phase, the interface is run for a set of compositions chosen to realise a composition meshing of the line defined above. Second, during the MEDICIS calculation, thermodynamic data are obtained by interpolation from the interface results. This two-step procedure avoids direct calls to the GEMINI2 code solver during the MCCI calculation itself and permits very fast calculations with MEDICIS code.

7.1.7.3.1. Coolability models

Top quenching models implemented or to be introduced soon in ASTEC/MEDICIS code are the following.

Before quenching onset of upper crust (bulk cooling), a standard scenario describing the upwards radiation without impact of gas bubbling at the upper interface temperature occurs towards the boiling water interface at saturation temperature; no description of transition boiling regime is done. After
onset of upper crust formation, power extracted at the upper crust interface is the sum of radiated power and of power extracted by water ingestion.

As alternative to the above mentioned standard scenario there is a boiling model available that calculates the heat flux through the top interface including a developing crust layer under consideration of a Nukiyama boiling diagram (Spengler, 2012).

Water ingestion treatment is based on the use of a Darcy’s law with a crust permeability value given as a user’s input.

The simplified model of melt eruption evaluates the melt entrainment using Ricou-Spalding’s correlation (Ricou & Spalding, 1961); a proportionality factor determining the ratio between volume rate of entrained melt and gas volumetric rate is a user’s input.

A more mechanistic model (Cranga, Mun, & Bottin, 2012) to be introduced in later ASTEC V2.1 version will include following features:


- Gas phase distribution: gas flow distribution between crust cracks and eruption holes is evaluated assuming the same pressure drop through the crust cracks and eruption holes.

- Hole geometry determination: constraints on hole diameter and hole density derived from following criteria based on energy balance, hydrodynamics and mechanics aspects: non-bulk freezing at the top of hole, non-bulk freezing of melt along hole lateral wall, limited lateral crust build-up along the hole, no gas build-up below the crust, non-flooding of holes by water, these models will be derived partly from more recently proposed models by M. Farmer (Farmer, Phenomenological Modeling of the Melt Eruption Cooling Mechanism during Molten Corium Concrete Interaction, 2006) and K. Robb (Robb & Corradini, 2010).

- Debris bed description.

Equations for the mass and energy balance equations and height evolution of debris layer are detailed hereafter:

\[ m_d = m_{\text{erup}} - m_{d->\text{crust}} \]  \hspace{1cm} \text{Equation 7.1-34}

\[ m_{\text{erup}}(h_{\text{erup}} - h_{\text{crust}}) = -(m_w \cdot c_{p_w} + m_d \cdot c_{p_d}) \frac{dT_{\text{sat}}}{dT} - P_{\text{crust,upwards}} + P_{d,\text{decay}} + m_{\text{erup}} \cdot h_{\text{cor}}(T_{\text{pool}}) - h_{\text{sat}}(T_{\text{sat}}) + m_d \cdot h_{\text{crust}}(T_{\text{crust}}) - h_{\text{sat}}(T_{\text{sat}}) \]  \hspace{1cm} \text{Equation 7.1-35}

where: \( m_{\text{erup}} \) is the flow-rate of vapour generated in the water pool determined from previous equation, \( m_w \) is the mass of water present in the debris zone, \( m_d \) is the mass of debris, \( P_{d,\text{decay}} \) is the decay power released in the debris bed, \( P_{\text{crust,upwards}} \) is the power transmitted by conduction from the upper crust up to the debris bed, \( m_{\text{erup}} \) is the mass transfer rate from the pool upper layer to the debris bed due to melt eruption, the ejected melt is supposed to be at the pool temperature, ignoring heat exchange with the upper crust, \( m_{d->\text{crust}} \) is the mass transfer rate of debris from the debris bed down to the upper crust defined below. \( h_{\text{cor}} \) is the average enthalpy of the corium in the pool, \( h_{\text{sat}} \) is the enthalpy of gases.

In case of dry-out due to an insufficient water flow rate, this dry-out is supposed to occur at the bottom of the debris-bed. The mass transfer rate of debris from the debris bed down to the upper crust is obtained from the following equation:
no dry-out heat flux limitation on debris cooling is taken into account, i.e. the debris bed remains cooled at saturation temperature as long as it remains quenched by water.

7.1.7.4. Code simplifications and limitations

Main code simplifications or limitations, excepted for the lumped parameter approach, are the following ones in the MEDICIS code:

1) For models in dry conditions:

- Te simplified structure of the pool/interface model where the slag layer may include a combination of real slag film, gas film and in case of the “no crust” model (model 2) crust fragments.
- Heat conduction is neglected behind the ablation front.
- Corium masses stored in crusts along the pool/concrete interface are not taken into account.
- Thermal inertia of crusts along the pool/concrete interface is neglected.
- In case of the upper crust, the crust mass increment is mixed with the whole crust mass and a composition averaged over the total crust thickness.
- Main transport properties of mixtures (viscosity excepted) are evaluated using a mass or volume averaging on individual species; this might be wrong for some transport properties such as heat conductivity and still more surface tension.
- No intermediate pool configuration between the homogeneous and stratified ones is possible; this causes in practice an overestimation of the time period during which the pool remains stratified.
- The attenuation of radiative heat flux above the corium pool by aerosols is ignored or, in case of the ASTEC version coupled to CPA containment module, evaluated crudely by a simple attenuation length given as a user’s input.
- A detailed description of heat transfer from the upper pool interface to surrounding walls is possible only if using the MEDICIS and ASTEC containment module CPA in a couple mode.

2) For models in wet conditions:

- In the present ASTEC version, the debris bed built-up during melt eruption is not explicitly described since ejected corium debris, once quenched, are mixed with the upper crust; this leads to an underestimation of the impact of top quenching on the decrease of ablation kinetics.
- No film boiling curve is taken into account to describe the upwards heat transfer during the top quenching phase.

3) For description of reactor pit geometry:

- Non-axisymmetric cavity shape (e.g. with a lateral corridor) cannot be described adequately.

4) Concerning the possible scenario:
• No lateral melt-through can be taken into account excepted manually with a reduction of corium inventory and a code restart.

• Successive corium pours from the vessel to the reactor pit can be described on a simplified way: corium poured from the reactor vessel is assumed to be spread over the whole pit area instantaneously; poured corium is distributed between oxidic and metallic layers; no direct heat transfer between the present water inventory and the poured corium falling through is assumed to occur.

• No corium spreading model is coupled with MEDICIS: in case of a multiple cavity concept, the pouring of corium from one cavity to another connected cavity can be described at least in some geometries (e.g. in EPR core-catcher); however in the general case, the spreading from a cavity to another connected cavity with a possible stop of the slug by freezing at the front cannot be treated.

7.1.8. **SOCRAT CODE**

7.1.8.1. **Pool interface models**

7.1.8.1.1. **Pool/concrete interface model**

The pool/concrete interface is not separated as definite material, specific boundary or so. The heat transfer through the interface is ruled by thermal conductivity, heat capacity and latent heat of the composite materials. Present interface model relies on user-defined properties (thermally dependent properties, Tsol & Tliq), composition of individual pool components (fuel, oxidised and non-oxidised metals, concrete) and chemical interaction model. Based on the components properties thermal conductivity of the compound in each FE is calculated. As a result the zones with different thermal properties appear that can be treated as representing mushy zone, crust or their combination. Since 2D temperature field is obtained on each step, FE state (solid or liquid) and properties are determined by its temperature. Gas film that can hamper heat transfer from the pool to the concrete is not considered.

The image shows heat conductivity in the area near the piece of melt-concrete boundary with white related to the values of 1-2 W/m*K in solid concrete and black related to the values of several hundred W/m*K in the melt pool. Here areas with different heat conductivity can be attributed to solid concrete, crust, mushy zone or melt.

![Figure 7.1-7: Diagram of crust modelling](image)

7.1.8.1.2. **Pool upper interface model**

Top crust description: pool/crust temperature, crust energy balance
Heat transfer at the top surface and upper crust boundary temperature is governed by the applied boundary conditions. Usually these are convective conditions

\[ F_{\text{conv}} = H(T - T_s) \]

and heat radiation conditions

\[ F_{\text{RAD}} = \varepsilon \sigma (T^4 - T_s^4) \]

for the boundary heat fluxes calculated from the "infinite" temperature or from the enclosure radiation heat exchange (HEFEST–EVA model). These boundary conditions are applied to each FE on the boundary thus allowing their spatial variation. If calculated temperature of some FE at the surface is below the solidus \((T_{\text{sol}})\) of the compound, this FE becomes solid. When all the FEs at the boundary become solid that is interpreted as crust formation. The crust thickness results from the calculated temperature field and upward heat flux. The crust elements may be referred separately for changing their properties in accordance with the definite physical model. A heat exchange with external coolant is ruled by heat transfer coefficient of 3rd kind condition, which is varied in accordance with the used coolant model. Heat flux from the crust (and other) boundaries is calculated during the stiffness matrix and load vector formation. Energy balance in the crust FEs is calculated in the frame of general procedure.

7.1.8.2. Thermalhydraulics models

7.1.8.2.1. Heat convection within a pool layer

The main tasks for the convective heat transfer model are: a) estimation of heat transfer to the melt boundaries; b) estimation of temperature across the pool.

*Mechanisms:* Spatially non-uniform effective orthotropic heat transfer coefficients (EOC) are used for modelling of convective heat transfer (Ozrin, Tarasov, Strizhov, & Filippov, 2010) (Filippov, E.V., & D.D., 2013). The EOCs are estimated on the base of averaged Nusselt number taken from empirical or CFD based correlations \(\text{Nu}(Ra)\) or \(\text{Nu}(Rai)\). Basic configurations for the identification of the pool volume shape are torispherical (hemispherical) molten pool and vertical cylinder (with internal heat generation or input heat flux). For the stratified molten pool in the reactor lower head the additional algorithms are used along with EOC model for profiling of sidewall heat flux.

*Correlations used:* \(\text{Nu}(Rai)\) correlations for torispherical (hemispherical) pools (BALI, COPO II Lo (Bonnet & Seiler, 1999), (Helle, Kymalainen, & Tuomisto, 1998). For the cylindrical molten pool at high \(Rai\) some CFD calculations results are applied ((Filippov, 2011), (Filippov, 2011b).

*Dependence on the interface orientation:* Interface orientation is taken into account implicitly in the form of the correlations \(\text{Nu}(Rai)\) used for estimation of heat transfer coefficients in the pool of particular shape.

*Recommendations on correlation choices and 2D distribution:* For curvilinear walls BALI correlations may be recommended as most reliable for high \(Rai\). But the bubbling contribution in the heat transfer seems to be very high, and the question about the correlation choice is open to discussion.

7.1.8.2.2. Heat convection between oxide and metal layers in a stratified pool

*Correlation used for convective heat transfer:* In oxide layer – BALI and COPO 2 Lo. Metallic layer in MCCI is located under the oxidic one and its heat transfer is ruled by bubbling – the above mentioned EOC is used with high axial conductivity and with the radial conductivity close to the nominal value. The additional EOC terms are based on Blottner model (Blottner, 1979). The available data on convective heat transfer in cylindrical pools are limited by the range \(Rai =< 1014\). For the
larger values of Rai the tested CFD results on natural convection are used (Filippov, 2011), (Filippov, 2011b).

Impact of possible oxide crust on heat transfer. The temperature of a finite element containing the melt and located near the melt boundary can decrease below the melting point. Then this element is treated as solid and its heat conductivity decreases. Thus the crust formation on the wall and at the oxide/metal interface and its influence on the heat transfer is treated automatically. If the element melts again, its heat conductivity increases effective and heat transfer coefficient is applied.

Recommendations on correlation choices and assumptions. The main assumption – the bubble dynamics doesn’t change significantly sidewall heat flux distribution in comparison with the conditions of the single phase high Rai experiments (BALI, COPO). This assumption should be checked on a wide range of experiments with large size pools (now most of them are not available for validation of HEFEST–EVA).

7.1.8.2.3. Stratification criteria

In current version the criteria are user-defined time and liquid fracture (for in–vessel calculations).

Minimum metal layer required for stratification occurrence – the thickness is greater than 2 cells of the mesh

Superficial gas velocity threshold is expected to be introduced in further versions for ex-vessel applications.

No recommendation on parameters of stratification criteria.

7.1.8.3. Thermophysical properties

Three main properties are used in stiffness matrix formation in FEM procedure: heat conductivity, specific heat, and density. The heat conductivity is represented by the effective orthotropic heat transfer coefficient mentioned above, which is calculated using material properties and Nu(Ra) correlations with some additional corrections. The coefficients depend on the pool aspect ratio and are different for horizontal and vertical EOCs. Here the viscosity is used only in calculations of Rayleigh number and may be constant or temperature dependent. In a liquid pool for each concrete component (SiO₂, CaO, MgO, etc.) as well as for core melt components (Fe, Zr, U and their oxides) a dependence of heat conductivity (which is based on conductivity for liquid phase) on temperature can be defined by user. In this way low heat conductivity values defined in input file allow accounting for silica and refractory materials in the melt.

Enthalpies of species are calculated from their heat capacities defined by user in input file. Mixture heat capacities are obtained as sum of weighted heat capacities of individual species.

7.1.8.4. Thermo-chemical properties

Thermo-chemical properties of melt components are originated from IVTANTHERMO database (Gurvich, Iorish, Chekhovskoi, & Yungman, 1993), (Gurvich, et al., 1989). Chemical reactions are accounted by an additional heat source or sink in the reaction area based on the standard heat of formation stored in the internal database. The properties of homogeneous mixtures are calculated by averaging. The thermochemistry solver is called every time step. The reactions are divided into interface and bulk types. No reactions on the metal–oxide boundary are considered. The eutectics and other nonlinear effects violating the averaging rules are taken into account separately. The effects of bubbling and of the composition on the viscosity aren’t yet accounted.

7.1.8.5. Coolability models

Top quenching models:
The interaction with the outer coolant is described by 3rd kind (Robin) boundary conditions as mentioned above: \( \lambda \frac{dT}{dt} = H(x,T,t)(T - T_{\text{out}}(x,T,t)) \). The physics of heat exchange is described by \( H(x,T,t) \) and \( T_{\text{out}}(x,T,t) \) values – they may be varied in accordance with the correlations built in HEFEST–EVA or may be calculated in external thermalhydraulics code (SOCRAT/RATEG), which may be coupled with HEFEST–EVA in the full SOCRAT calculation. In particular, for VVER core catcher the correlation (Lopukh, Loginov, & al., 2000) for the heat transfer coefficient \( H(x,T,t) \) is used for taking into account film boiling and radiation heat exchange through the film during the melt top flooding.

The radiative heat transfer is ruled by radiation boundary condition \( \lambda \frac{dT}{dt} = \varepsilon(x,T,t)(T^4 - T_{\text{out}}^4(x,T,t)) \) where \( T_{\text{out}}(x,T,t) \) may be user's defined or calculated in built-in enclosure radiation procedure (including the calculation of view factors matrix for quasi-cylindrical cavities). No effects of gas bubbling are considered. No transition regimes are considered in quenching modelling – the heat exchange with the coolant starts at definite time and the boiling is always assumed to be film boiling.

After onset of upper crust due to quenching Water ingestion and melt eruption can be accounted by user input as above mentioned heat removal coefficient \( H(x,T,t) \). No internal evaluation of heat removal increase due to water ingestion and melt eruption is performed in current version.

Debris description

No porosity is supposed in arriving from the RPV debris (melt). The model of its arrival is of “donor-receiver” type. The geometry of arriving debris is prescribed by the mesh partitioning (the order in which different cavity areas are filled is defined in input data). The boundary between the melt and the solid debris moves when the solid elements melt and finally disappears when whole debris is molten. Mass and energy balance are monitored in the same way as in all material relocation procedures.

7.1.8.6. Code simplifications and limitations

Main code simplifications or limitations are those expected for the 2D approach without solution of the flow equations for the melt. For SOCRAT/HEFEST-EVA code they are the following.

1) For models in dry conditions:

- Concrete reinforcement is modelled as uniformly distributed steel component, in the same way as for other inhomogeneities. Mixture properties are used for multicomponent materials.
- Crust treatment requires relatively thin meshing of the significant part of a calculation domain that increases total CPU time.
- Melt relocations and convective heat transfer are treated simplistically, without solution of flow equation; that is implemented as:
  a) Melt relocation (arrival, stratification, spreading) is modelled as quick (one time step) change of melt configuration.
  b) Heat transfer in the melt is modelled by effective orthotropic heat transfer coefficients, which are estimated on the base of the known correlations obtained for homogenous pool (without stratification and bubbling) of definite shape.
- The correlation based approach demands verification (CFD) against representative set of configurations.
• Transition from the correlations used for cylindrical cavity or for torispherical (hemispherical) cavity lacks experimental support. (The correlations, being developed on the basis of pools with fixed shape and size, may be incorrect for MCCI pools with changing configurations).

• The effect of bubbling on heat transfer is not considered in present version.

2) For models in wet conditions:

• Melt eruption and water ingestion can be accounted for only by user defined boundary conditions.

• No film boiling curve is taken into account to describe the upwards heat transfer during the top quenching phase.

3) For description of reactor pit geometry:

• 2D approach for reactor pit geometry: that allows only axisymmetric configurations – corridors and other non-axisymmetric elements can be treated by introduction of space with equivalent area of melt-concrete interface or by solution of coupled problem in which the surrounding compartments are modelled separately/.

4) Concerning the possible scenario:

• Successive corium pours from the vessel to the reactor pit is described on a simplified way: corium poured from the reactor vessel is assumed to be located at the definite places, which are now prescribed by the code user.

• Poured corium is distributed between oxidic and metallic layers; no direct heat transfer between the present water inventory and the poured corium falling through is assumed to occur.

• No one-run model of corium spreading along the basemat.

5) Chemistry model

• The reactions are conservative in terms of \( H_2 \) and CO generation – the only limiting factor is reagents availability.

7.1.9. TOLBIAC-ICB CODE

7.1.9.1. Pool interface models

7.1.9.1.1. Pool/concrete interface model

A crust exists at the pool/concrete interface. In the frame of the hypothesis of thermodynamic equilibrium, the phase segregation model is used (Seiler J. -M., 1996), (Seiler & Froment, 2000). The interfacial temperature \( T_{\text{int}} \) between melt and crust is then the liquidus temperature calculated by GEMINI2, using the evolving melt composition.

The GEMINI2 code is developed by THERMODATA (Cheynet, 2007), and gives thermodynamic equilibrium between species, by mean of minimisation of Gibbs energy. The data base that is used by GEMINI2 is NUCLEA. As an option for fast calculations, the code user may select the use of pseudo-binary laws, fitted from GEMINI2 results, which gives formulas for the liquidus temperature depending on the concrete composition. By default, the crust composition is calculated through a coupling to GEMINI2, the code user can decide to consider a homogeneous solidification (crust layer having the composition of pool at the time of its formation) or he can use pseudo-binary laws to calculate its composition. The crust is calculated assuming a steady state conduction regime (a transient regime can be used).
7.1.9.1.2. Pool upper interface model

A crust generally appears at the upper surface of the pool because of radiative heat transfer towards the reactor cavity or water aspersion. Crust thickness and crust surface temperature are calculated, supposing either a steady state regime or a transient crust growth. The melt to crust heat exchange is governed by the heat transfer coefficient and the interfacial temperature (pool liquidus temperature), and does not depend on the external conditions. The crust surface temperature and crust thickness depend on the external conditions, which may be radiation or heat exchange with a water layer. When there is no water aspersion, the crust density is larger than the melt density (crust composed of refractory materials), and the crust continually forms and sinks: it is then only taken into account for the heat transfer, and no crust increase is taken into account. In case of water aspersion, a crust porosity is supposed, and the crust increase is taken into account in the model.

For the radiation above the melt surface, two levels of modelling exist in TOLBIAC-ICB. The most simple radiation model only considers a radiation between two infinite planes, the upper crust and the surroundings, depending on the surroundings temperature and the emissivity of the pool surface and of surroundings. A more complex model also exists. The cavity above the melt level is described, form factors are considered in the radiation model, and ablation of the concrete vertical walls above the melt level is taken into account. The ablation of a horizontal steel wall representing the vessel structure above the melt may also be modelled. Heat losses through gas or aerosols escaping from the melt pool to the containment may be taken into account through a coefficient of power loss defined by the code user.

7.1.9.2. Thermalhydraulics models

7.1.9.2.1. Heat convection within a pool layer

The code user may select several different heat transfer correlations on each boundary as quoted above in section 3.2.9. The code user may also simply modify the ratio between the bottom and lateral heat transfers. The recommendation is to use weighting factor rather than very different correlations, written in terms of different variable groups, since this last solution may lead to unexpected variations of the power split.

7.1.9.2.2. Heat convection between oxide and metal layers in a stratified pool

In case of stratified configuration, the Greene and Irvine’s correlation (Greene & Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988) at the interface between the two-melt layers is recommended.

7.1.9.2.3. Stratification criteria

The code user can choose between three criteria for stratification: that from BALISE test performed at CEA (Tourniaire, Seiler, & Bonnet, 2003), Epstein (Epstein M., Petrie, Linehan, Lambert, & Cho, 1981), or BALISE and Epstein together. Details of the criteria are given by Tourniaire and Bonnet (Tourniaire, Seiler, & Bonnet, 2003). The BALISE criterion depends only on the density of the liquids, whereas the Epstein criterion depends on the densities and also of the height of the layers.

For the simulant fluids used in the BALISE experiment, the BALISE and Epstein criteria give results of the same order. But, when applied to reactor cases, the tendency is different with a delayed stratification in case of the Epstein criterion, due to the low thickness of the metallic layer. The criteria are as follows:

BALISE

Stratification occurs if the gas superficial velocity $J_g$ is lower than a limit velocity $J_{g,lim}$:

\[
J_g < J_{g,lim}
\]
where index \( H \) corresponds to the heavy liquid and \( L \) to the light liquid.

It must be noticed that only the gas superficial velocity corresponding to the bottom concrete ablation is used in the criterion. The gas generated by lateral concrete ablation is not supposed to participate to the global motion in the pool, but only to flow along the lateral wall.

**EPSTEIN**

For a homogeneous melt, stratification occurs if the equivalent density \( \rho_{eq} \) after heavy and light oxide mixing is greater than the density of the light liquid \( \rho_L \), with:

\[
\rho_{eq} = \frac{\rho_H \cdot (1 - \alpha)}{1 + V_L / V_H} \left(1 + \frac{\rho_L V_L}{\rho_H V_H}\right) \quad \text{Equation 7.1-38}
\]

where \( V \) correspond to the volume of the layers.

For a stratified melt, mixing occurs if the equivalent density \( \rho_{eq} \) of heavy oxide with void is lower than the density of the light liquid \( \rho_L \), with a different equivalent density:

\[
\rho_{eq} = \rho_H \cdot (1 - \alpha) \quad \text{Equation 7.1-39}
\]

**BALISE and EPSTEIN:**

In this case, both criteria \( J_g \) lower than \( J_g \lim \) and \( \rho_{eq} \) greater than \( \rho_L \) must be satisfied.

**Additional conditions for the stratification and the de-stratification of the corium pool:**

Another condition for the de-stratification of the pool is still used in the present version of the code: when the pool is stratified, if the thickness of the liquid metal layer becomes lower than 3 cm, then the de-stratification of the pool takes place.

**7.1.9.3. Thermophysical properties**

Physical properties are not of minor interest, since they are used for determination of melt height and heat transfer areas (density), and also for heat transfer coefficients and void fraction model. They are also used in the stratification model to determine oxidic/metallic pool configuration. The physical properties of individual species used in TOLBIAC-ICB are mainly those defined in the frame of the ECOSTAR European project (Journeau, 2003) for the densities, and also in the CORPRO data base of CEA (Piluso, 2003) for specific heat, conductivity and surface tension.

The density of the melt mixture is obtained by a weighting in volume, whereas the heat capacity, conductivity and surface tension are obtained using weighting in mass. The dynamic viscosity of the melt is calculated following the method proposed by (Seiler & Ganzhorn, Viscosities of corium concrete mixtures, 1997), (Sudreau, Ramacciotti, Cognet, Seiler, & Journeau, 2000) for mixture with silica above liquidus temperature and (Ramacciotti, Journeau, Sudreau, & Cognet, 2001). The Urbain model is used depending on the mole concentration of the species in the mixture. The species are divided into network former (SiO2), modifiers (CaO, FeO, MgO, UO2, ZrO2) and amphoteric (Al2O3, Cr2O3, Fe2O3, NiO). In case of a metallic layer, the Andrade model is used.
7.1.9.4. Thermo-chemical properties

At time step n, the data for the considered melt are the mass of each species, and the temperature of the melt for a homogeneous melt, or the interfacial temperature for a stratified melt. The basic hypothesis is that thermodynamic equilibrium between the species occurs. An instantaneous equilibrium is supposed. The resulting distribution of masses between solid and liquid phases after equilibrium is given using pseudo-binary diagrams (default) or by GEMINI2 (optional).

7.1.9.5. Coolability models

In case of water aspersion, both radiation and convection with water are taken into account. The convective heat transfer coefficient is derived from the boiling curve of water.

Melt ejection models are implemented in the code. The PERCOLA model is derived from the PERCOLA experiments performed at CEA-Grenoble (Tourniaire & Seiler, 2004). In this model, the ejection rate mainly depends on the superficial velocity of the gas issued from the concrete ablation. Due to the large span of the reactor pit, the upper crust is supposed to float on the corium pool (no sticking to the sidewalls). However, the code user has to define the number of holes in the crust per square metre, the size of the holes and the crust porosity. The simpler Ricou and Spalding (Ricou & Spalding, 1961) model may also be used, with one single user’s input coefficient.

A limitation of the debris bed height is taken into account, considering the dry-out limit. If the dry-out limit is reached, equilibrium is supposed between the particles that are ejected, and the particles that re-melt into the pool. The dry-out heat flux $\varphi_{do}$ is calculated using the correlation of Lipinski (Lipinski, 1980):

$$
\varphi_{do} = 0.245 \frac{H_{LS}}{D^3} \left( \frac{\rho_L \rho_v \rho_D \rho_L^2 (1 - \varepsilon)}{1 + \rho_L / \rho_v} \right)^{0.5}
$$

where, $H_{LS}$ is the latent heat of water, $\rho_L$ and $\rho_v$ the density of liquid water and of steam, $D$ the mean diameter of the particles in the debris bed, and $\varepsilon$ the porosity of the debris bed.

The maximum height $e_{max}$ of the debris bed is obtained when the dry-out heat flux corresponds to the heat flux out of the pool $\varphi_b$, plus the internal heat of the debris $Q_d$:

$$
\varphi_{do} = \varphi_b + e_{max} Q_d \quad \text{Equation 7.1-41}
$$

Recently the water ingestion model of Lomperski and Farmer (Lomperski & Farmer, 2007) was also implemented.

In case of water quenching, the mass flow rate of steam formed due to the heat transfer to water is calculated. The heat source is the heat flux out of the crust on one hand, and the decay power in the debris bed above the crust on the other hand. Sensible heat from the water temperature $T_{wat}$ to the saturation temperature is considered, together with the latent heat of water $H_{LS}$. Finally, the steam mass flow rate is given by the following relation:

$$
Q_{steam} = \left( S_{surf} \varphi_{surf} + Q_{e,vol} \right) / \left[ H_{LS} + C_{p,water} (T_{sat} - T_{wat}) \right] \quad \text{Equation 7.1-42}
$$

7.1.9.6. Code simplifications and limitations

Main code simplifications or limitations are the following:

1) For models in dry conditions:

- The TOLBIAC-ICB code assumes simplified academic (steady state) configurations: phase macrosegregation, interface exactly at the liquidus temperature, either fully mixed or totally stratified configurations.
Ablation anisotropy of silica-rich concretes by the oxidic phase is modelled by a simple experimentally-fitted coefficient increasing the lateral heat transfer coefficient.

2) For models in wet conditions:

- The PERCOLA model can be used to calculate the melt ejection but the code user has to define the number of holes in the crust per square metre, the size of the holes and the crust porosity.

3) For description of reactor pit geometry:

- A lateral corridor can be considered.

4) Concerning the possible scenario

- Lateral melt-through is taken into account when a corridor is modelled. It is also possible to consider a reduction of corium inventory during the calculation.
- Successive corium pours from the vessel to the reactor pit can be described on a simplified way: corium poured from the reactor vessel is assumed to be spread over the whole pit area instantaneously; poured corium is distributed between oxidic and metallic layers; no direct heat transfer between the present water inventory and the poured corium falling through is assumed to occur.

7.1.10. WECHSL code

7.1.10.1. Pool interface models

7.1.10.1.1. Pool/concrete interface model

The pool/concrete interface temperature is the freezing temperature $T_{\text{freez}}$, $T_{\text{sol}} \leq T_{\text{freez}} \leq T_{\text{fimmob}}$, of the oxide melt at which a stable crust starts to form. Steady-state heat conduction is assumed for thin crusts, whereas in case of thick crusts the one-dimensional transient heat conduction is modelled.

Both the crusts and the liquid melt have the same composition. Between the crust and the concrete a gas film exists.

7.1.10.1.2. Pool upper interface model

This section is intentionally left blank.

7.1.10.2. Thermal-hydraulics models

7.1.10.2.1. Heat convection within a pool layer

The heat transfer between the melt bulk and the inside of the crust is determined by a discrete bubble type heat transfer mechanism, with the driving temperature difference determined by the bulk temperature and the freezing temperature, $T_{\text{freez}}$, of the melt layer at the crust interface.

The modelling of the heat transfer from the melt to the concrete is based on the following ideas. The heat transfer from the melt bulk to the concrete is characterised by processes forming boundary layers at the melt pool surface facing concrete. The most important process which governs the heat transfer phenomena is the release of large volume fluxes of gases from the decomposing concrete. If the superficial velocity of the gases being released from the concrete is sufficiently high, a stable gas film is formed between the melt and concrete. If the superficial gas velocity drops below a limiting value, the heat transfer will be governed by a nucleate boiling type of discrete bubble gas release. As the melt is intensively stirred by the gases released during concrete ablation, the bulk of each layer of the melt is assumed to be isothermal. Because of the high thermal conductivity and the low viscosity of the metallic phase, the temperature drop across the boundary layer of the melt layer is very small. For an oxide melt with a high Prandtl number, $Pr > 1$, the temperature drop across the boundary layer
is quite significant. Due to cooldown of the melt, the temperature in the melt boundary layer facing the concrete may drop below the liquidus temperature, which characterises the onset of solidification. The initially thin viscous layer will grow and the solid volume fraction will increase with a further temperature decrease. The strong variation of the viscosity with temperature in the temperature boundary layers at the interfaces of the melt considerably reduces the heat transfer from the pool. A correlation proposed in (Foit, Reimann, Adroguer, Cenerino, & Stiefel, 1995), (Foit & Miassoedov, 1995) is used for correcting the appropriate non-dimensional heat transfer parameter, i.e.

\[
Nu = Nu_0 \left[ 0.645 \left( \frac{\mu_i}{\mu_b} \right)^n + 0.356 \right], \quad n = -2 \quad \text{Equation 7.1-43}
\]

Nu is the corrected Nusselt number and Nu0 the constant property solution. The viscosity \( \mu_i \) is the viscosity at the interface temperature, while \( \mu_b \) is evaluated at the bulk temperature. The most pronounced rheological changes with temperature occur at temperatures for which the solid volume fraction, \( \text{fimmob} \), passes through the critical range of 50-80 vol.%. This temperature corresponds to the freezing temperature, \( T_{\text{freez}} \) defined above in Section 7.1.10.1.1.

7.1.10.2.2. Heat convection between oxide and metal layer in a stratified pool

This section is intentionally left blank.

7.1.10.2.3. Stratification criteria

This section is intentionally left blank.

7.1.10.3. Thermophysical properties

The viscosities of the oxide melt below the liquidus temperature are assumed to follow the modified Pinkerton-Stevenson correlation. The results obtained with the modified correlation are in good agreement with the viscosity measurements performed for the melt used in the KATS tests.

7.1.10.4. Thermochemical properties

For the oxide phase and the dispersed melt the solidus and liquidus temperatures are determined either from a quasi-binary phase diagram or from a user input table. The solidus and liquidus temperatures of the metallic phase in WECHSL are calculated from the chromium-nickel-iron, zirconium-iron and silicon-iron phase diagrams.

7.1.10.5. Coolability models

The only coolability model is used in WECHSL is the film boiling model.

7.1.10.6. Code simplifications and limitations

1) For models in dry conditions:
   - The current level of validation (Foit, Reimann, Adroguer, Cenerino, & Stiefel, 1995) against experimental data ACE, SURC, BETA, COMET-L, VULCANO, MOCKA, CCI test series (see Section 2 for references) would appear to be good enough to justify the use of the MCCI codes such as WECHSL for risk assessment studies, provided the uncertainties in the predicted results are taken into account.

2) For models in wet conditions:
   - No water ingression or melt eruption models

3) For description of reactor pit geometry:
   - Non-axisymmetric cavity shape (e.g. with a lateral corridor) cannot be described adequately.
7.2. Appendix on comparison of code validation status and uncertainties

For the most important phenomena the individual validation status is detailed (incl. the conclusions e.g. which phenomena are adequately validated and which require more validation) and individual uncertainties are outlined.

7.2.1. Concrete ablation and 2D cavity profile

7.2.1.1. COCO

The results of the comparison between COCO code and COTELS D-6 test and NEA-CCI-3 are shown here as examples.

A core debris simulant (stainless steels in case of the D-6) was melted in an electric heating furnace and it dropped into the concrete vessel set up in the pressure vessel. The concrete vessel was equipped with a high frequency induction heating coil surrounding it to heat up the melt. The axisymmetric concrete vessel was made of basalt concrete. A number of thermocouples were installed to detect the ablation front. The input to the high-frequency induction coil was about 80 kW and the heating efficiency is 30%. Therefore, the heating rate of the melt was around 24 kW. The test continued 130 minutes.

Figure 7.2-1 left shows the observed result of a concrete vessel after the test. There was an ingot of stainless steel under the glass-like clinker. The concrete was ablated about 160 mm downward and 55 mm laterally. Figure 7.2-1 right shows the calculated ablation profile. In the calculation, a stratified layer model of metal was used, as the difference in density is large for metal and concrete. Since the heat transfer coefficient of metal layer in the calculation is very large, the downward ablation is larger than side wall ablation. The calculated profile is similar to the observed one.

Figure 7.2-1: Stratification of molten oxide and metal after test D-6 of COTELS project

The position of ablation fronts are shown in Figure 7.2-2. The plots are test results and the lines show the maximum of the calculated vertical and horizontal ablation depth. The boxes at 130 min are the depth measured after teardown. The calculated ablation depth fits to the test results. (As the ablation was not uniform, the test results are scattered).
The CCI-3 experiment of the NEA MCCI Project is a two-dimensional corium concrete interaction test. The cross-sectional area of the cavity was 50 cm (distance between the tungsten electrodes) x 50 cm (distance between the concrete sidewalls). The basemat and sidewalls were made of siliceous concrete. The initial melt contained 15 wt.-% concrete components. The input to the direct Joule heating electrodes was about 120 kW and the test continued for 146 minutes.

The fundamental principle of deriving the scaling law for simulating the rectangular cavity by axisymmetric code is that the following ratios are the same in the test and the analysis:

1. Concrete sidewall surface area/basemat surface area
2. Total surface area of concrete/melt mass
3. Input power/melt mass
To satisfy these relations, the scaling laws (values in the analysis/values in the test) are:

- Coordinate in vertical direction, ablation depth = 1.
- Intensive variables (temperature, volumetric heat rate) = 1.
- Extensive variables (mass, total power) \(=(r/y) \pi = \pi\).

where \(r\): cavity radius in calculations (\(r=\)distance between sidewalls for CCI-3), \(y\): \(=\)distance between electrodes.

The calculated ablation profile after the test is shown with the test results in Figure 7.2-3. The north and south side wall ablations in the test differ a little in the depth. The calculated profile is close to the test results.

7.2.1.2. **CORQUENCH**

In terms of the ability to capture 2D cavity erosion behaviour, the code has been compared to NEA MCCI tests CCI-2, -3, -4, and -6. For tests conducted with high gas content concretes such as limestone-common sand (i.e. CCI-2 and -4), the code does a reasonable job of predicting isotropic ablation behaviour, but does not perform as favourably for siliceous concrete tests (that exhibit strong lateral ablation behaviour) when default heat transfer correlations are used. However, the code does seem to perform better when the gas film breakdown modelling option is employed. This assumption leads to better agreement for siliceous concrete tests.

7.2.1.3. **COSACO**

The validation results show an adequate consistency with the experimental results regarding concrete ablation behaviour and melt temperature (see Section 7.2.2.3). As an example, the ablation depth history as calculated for the MACE M4 test and CORESA 2.1 test are given in Figure 7.2-4 in comparison to the experimental data. Furthermore, the results of various benchmark calculations show good consistency with other MCCI codes.

On this basis, it is concluded that the general treatment of thermochemistry in COSACO and the thermal-hydraulic models specifically implemented for the oxidic and metallic melts are well-suited to analyse the characteristics of the MCCI process.

![Figure 7.2-4: COSACO-validation: comparison of calculated and measured evolution of the concrete ablation](image-url)
7.2.1.4. **MAAP**

ACE tests and SURC-4 test are 1D experiments in which ablation can occur only in the downward direction, while BETA tests are 2D experiments in which the ablation can occur in both the sideward and downward directions. In general, ablation rates predicted by MAAP model compared better for the ACE and SURC-4 tests than the BETA tests. Figure 7.2-5 shows an example of the ablation depth calculated by MAAP compared with the ACE-L2 test data:

![Figure 7.2-5: MAAP-validation: Comparison with ablation depth data for ACE-L2](image)

Ablation rates measured in the BETA tests were appreciably higher than those in ACE and SURC-4 tests. Although the BETA tests allow the ablation to occur in both directions, post-test examinations revealed the ablation in the downward direction is much larger than that in the sideward direction. It was postulated that the high ablation rate in the downward direction was caused by spalling in the concrete (Epstein, 2004). The relatively smaller sideward ablation may be caused by the formation of a foamed layer between the corium and the sidewall which separated the corium from contact with the wall. It was difficult for the MAAP model to simulate this type of behaviour, since a spalling model was not implemented (such a phenomenon may not be prototypic in a nuclear plant set up), and the foamed layer model was not included.

The MCCI and corium coolability models in the latest released version MAAP5.02 have been compared with CCI tests, which are the latest 2D dry and wet cavity tests, performed by Argonne National Laboratory under the sponsorship from NEA (Farmer, Lomperski, Kilsdonk, & Aeschlimann, OECD MCCI Project: 2-D Core Concrete Interaction (CCI) Tests, Final Report, 2006). Two in the total of six tests have been selected for comparisons: CCI-2 and CCI-3, where the concrete for the CCI-2 test is limestone-common-sand (LCS) concrete, and the concrete for CCI-3 test is European siliceous concrete. For each test, MAAP5 calculations were performed with two sets of inputs for the convective heat transfer coefficients and exponent (see section 7.1.6), as shown in the following table:

<table>
<thead>
<tr>
<th>Tests</th>
<th>Set 1: Varying Heat Transfer Coefficients</th>
<th>Set 2: Constant Heat Transfer Coefficients</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$h_w$ W/(m²K)</td>
<td>$h_s$ W/(m²K)</td>
</tr>
<tr>
<td>CCI-2</td>
<td>3 500</td>
<td>3 500</td>
</tr>
<tr>
<td>CCI-3</td>
<td>1 000</td>
<td>3 500</td>
</tr>
</tbody>
</table>

The inputs for the set 1 assumes the heat transfer coefficients from the molten corium to surround crusts vary with the solid fraction in the corium, while the inputs for the set 2 assumes the heat transfer coefficients are constant.
Figure 7.2-6 shows comparisons of ablation depths between MAAP calculations and the experimental data. The calculated ablation depths are in reasonable agreement with the experimental data.

![Image of comparisons between MAAP calculations and experimental data]

(a) CCI-2 Comparisons  
(b) CCI-3 Comparisons

**Figure 7.2-6:** MAAP-validation: Comparison of concrete ablation depths for CCI tests

7.2.1.5. **MEDICIS**

In the parametric approaches for the heat transfer coefficients used by IRSN and GRS, the heat transfer is referred to the impact of an interface structure between melt and concrete (either by introducing a significant thermal resistance \( h_{\text{slag}} \) between melt and concrete as function of interface angle or by using effective heat transfer coefficients). Here the dependence of this thermal resistance versus the interface orientation determines directly the ablation profile and the final cavity shape. For the several experiments with an LCS type of concrete a uniform thermal resistance along the interface seems the best estimate whereas for several experiments with siliceous concrete it is suggested to account for the anisotropy of the 2D ablation observed in several experiments by using an increased thermal resistance in the downward direction (see Figure 7.2-7 and Table 7.2-2).

**Table 7.2-2:** Comparison of heat transfer coefficients in the approaches of GRS and IRSN

<table>
<thead>
<tr>
<th>Concrete type</th>
<th>model type</th>
<th>heat transfer at bottom interface (W/m²/K)</th>
<th>heat transfer at lateral interface (W/m²/K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>LCS concrete</td>
<td>IRSN ‘no crust model’</td>
<td>300</td>
<td>300</td>
</tr>
<tr>
<td></td>
<td>GRS global heat transfer bulk to interface</td>
<td>200</td>
<td>200</td>
</tr>
<tr>
<td>siliceous type concrete</td>
<td>IRSN ‘no crust model’</td>
<td>80</td>
<td>300</td>
</tr>
<tr>
<td></td>
<td>GRS global heat transfer bulk to interface</td>
<td>80</td>
<td>300</td>
</tr>
</tbody>
</table>
This simplified approach does not track the existence of a crust at the interface, but rather gives an approximation of the overall effective heat transfer coefficient. Detailed mechanisms of the heat transfer are not identified. It is presently not clear how thermal resistances vary with time, i.e. with the melt bulk conditions (viscosity, superficial gas velocity, decay power) in the pool. There are indications that effective heat transfer coefficients may be nearly constant throughout the experiments or slowly decreasing, except for the initial phase in the experiments. The 2D heat flux distribution is then mainly controlled by the 2D relations of the heat transfer coefficients to the different interfaces. Most experiments are overestimated with regard to the integral eroded volume but are met satisfactorily with regard to maximum erosion depths. The validation of the 1D experiment MACE-M3b performed by GRS shows that the 2D set-up of the model can also be applied for a 1D scenario (Spengler, 2012).

In IRSN approach the heat transfer to the pool/concrete interface is thus evaluated as a combination of heat convection within the pool and of a thermal resistance at the pool/concrete interface, without any stable crust. The anisotropy in heat flux distribution observed in tests with siliceous concrete causing a high corium viscosity is explained only by the non-uniform profile of thermal resistance along the pool/concrete interface. The convective heat transfer coefficient is kept constant and does not depend on the interface orientation. This view is supported by the first CLARA results on the heat convective coefficient distribution in a bubbling pool. These results (see Figure 7.2-8) show indeed that the ratio of lateral to axial convective heat transfer coefficients stays near one at high viscosity and exceeds largely one only at low viscosity where the convective heat transfer coefficient becomes high and cannot impact on the effective heat flux distribution along the pool/concrete interface because of the much lower conductance of the interface itself.
7.2.1.6. **SOKRAT/HEFEST**

Since SOCRAT/HEFEST is a fully 2D code, it should correctly predict concrete ablation both in 1D and in 2D geometry. 1D experiments of SURC (1-4), SWISS (1-2) and ACE (L2, L5, L6, L8) series were chosen for the validation matrix. Among the 2D experiments COMET (L2, L3) and BETA (V3.2, V3.3) are of particular interest due to the different behaviour of the concrete of different types. It can be noted that even in 1D experiments the fully 2D approach can be useful due to the effects of the crucible walls and of inhomogeneous heating.

7.2.1.7. **TOLBIAC-ICB**

The analyses of the results of the MCCI oxidic tests (NEA-CCI-2,-3,-4 and -6; VULCANO VB-U5, -U6) show that there can be anisotropic ablation rates depending on the composition of the concrete. For silico-calcareous and siliceous concrete, the radial ablation rate is larger than the radial one. As this phenomenon is not well understood, a parameter is introduced in TOLBIAC-ICB in order to impose a higher heat transfer coefficient in the radial direction than the axial one. The value of this parameter is advised from 1 (for calcareous concretes) to 3 (for siliceous concretes). A result of a calculation performed with TOLBIAC-ICB on VULCANO VB-U5 is presented on Figure 7.2-9.

![Figure 7.2-8: Ratio of lateral to axial convective heat transfer coefficients versus pool viscosity deduced from the small-scale CLARA tests](image-url)
7.2.2. Melt temperature

7.2.2.1. COCO

The results for CCI-3 are shown here as example. Figure 7.2-10 shows the temperature transients of the melt in the measurement and COCO calculation. The temperature by COCO agrees well with the measurements.

The calculated concrete concentration and melt temperature are plotted at every time step on the phase diagram which is used for the COCO analysis as shown in Figure 7.2-11. The plotted point horizontal value means the averaged concrete fraction for melt and crust. As shown in Figure 7.2-11, the melt is in a two phase regime of solid and liquid through the test duration.
7.2.2.2. CORQUENCH

Melt temperature has been measured in all experiments against which CORQUENCH has been validated, and the temperature predictions by the code have been compared with the data. In general, the code seems to reasonably capture the magnitude and trend of the test data.

7.2.2.3. COSACO

As for the concrete ablation, the melt temperature as calculated by COSACO has been verified against a large number of experiments, as e. g. shown in Figure 7.2-12 for the MACE M4 and CORESA 2.1 tests.

7.2.2.4. MAAP

Melt temperatures were compared between MAAP predictions and dry cavity tests (ACE, SURC and BETA tests) mentioned in the previous section. The temperature calculated by MAAP is the average temperature of the corium pool, while the temperature measured in the experiments is the temperature in the molten pool. For dry cavity cases, the two temperatures do not significantly differ from each other. Figure 7.2-13 shows an example of the comparisons between MAAP and the ACE-L2 test.
In general, the temperatures compared better than the ablation rates. Even the temperatures in the BETA test series showed acceptable agreement with the experiment data.

Figure 7.2-14 shows the comparisons of the melt temperatures between MAAP5.02 calculations and CCI-2 and CCI-3 data. As shown, the temperatures calculated with the constant heat transfer coefficients have better agreement with the data than those calculated with the varying heat transfer coefficients.

When using an effective thermal resistance at the melt/concrete interface as proposed by IRSN and GRS it is observed that the long term melt pool temperature in the calculation is governed by the effectiveness of heat transfer processes and conditions at the interfaces (including also the top interface) and by the evolution of interface area. Validation calculations performed by IRSN consider a detailed model for the evolution of a top crust on the corium pool, in which the interface temperature between pool and upper crust is determined from a volumetric molten corium fraction equal to around 0.5. Typical calculation results are shown in Figure 7.2-15 and Figure 7.2-16. The agreement with experimental data (slight underestimation of pool temperature in CCI-2 and overestimation at the opposite in CCI-3) is rather good in spite of the simple and consistent assumptions used for the heat transfer at the pool/concrete interface in both tests. This gives hints that the assumption of stable crust at the melt/concrete interface with an imposed pool/crust interface temperature is not required to reproduce experimental data. A main conclusion is that the pool temperature is only weakly linked to
the melt liquidus temperature; the link between the pool temperature and the liquidus temperature is suspected only through the liquid fraction in the melt, which equilibrates to a large enough value to permit convective heat transfer to the pool interfaces. A second conclusion is that the heat transfer between melt and concrete may be explained without considering the evolution of a crust along the pool/concrete interface.

7.2.2.6. SOKRAT/HEFEST

All experiments noted in Section 7.2.1.6 except for COMET (L2, L3) and SWISS are used in verification of the SOCRAT/HEFEST ability to predict the melt temperature. Those experiments, where abrupt temperature changes due to chemical reactions were observed, are of special interest here. It concerns the SURC series and some of the ACE experiments.

More abrupt temperature rises during chemical reactions are expected in HEFEST simulations due to the limitations of the chemistry model, especially in those experiments where Zr was added to a silica-rich melt like in SURC-4.

7.2.2.7. TOLBIAC-ICB

Typical calculation results are shown in Figure 7.2-17 and Figure 7.2-18. The calculation of CCI-2 is performed considering that all chromium is oxidised and the initial temperature is taken as the liquidus temperature of this initial mixture. The results show that the temperature is well predicted by the code (except at the early beginning of the experiment).

The calculation of CCI-3 shows a short decrease of the melt temperature in the experiment. This is not reproduced by TOLBIAC-ICB calculation which over-predicts the melt temperature. This is due to the assumption that the temperature at the interface between the melt and the crust is equal to Tliquidus. In case of a silico-calcareous (or a siliceous) concrete, this hypothesis gives higher melt temperature.
7.2.3. **Gas release**

7.2.3.1. **CORQUENCH**

CORQUENCH predictions of melt superficial gas velocity have been compared with data obtained in ACE/MCCI tests L2, L5, L6, and L8. The code seems to reasonably capture the magnitude and trend of the gas release in the tests. However, the timing of peaks associated with ablation through different zones in the basemat that contain varying amounts of gas (i.e. concrete-metal inserts vs. pure concrete) do not always line up due to the fact that the code does not precisely predict the time at which a given boundary is reached.

7.2.3.2. **COSACO**

The gas release is directly linked to the concrete erosion history. Thus the validation work has mainly focused on concrete erosion.
7.2.3.3. **MAAP**

Gas release is calculated by MAAP models, but has not been compared with the experiments.

7.2.3.4. **MEDICIS**

The emphasis on validation work has recently been on the 2D ablation behaviour. The gas release coming from the decomposition of the concrete is regarded only as subsequent effect of (2D) concrete ablation. However, for MACE-M3b the phenomenological models for gas releases of the species CO, CO2 and H2 are successfully validated by GRS. Based on this it is assumed that the models for metal oxidation in MEDICIS give realistic predictions.

7.2.3.5. **SOKRAT/HEFEST**

The same list of experiments as in Section 7.2.1.6 is used for the verification of gas release simulation. Here, an influence of Zr-oxidation on the gas flow rate and the composition is of special interest, so the focus is mainly on the SURC series. Faster and shorter H2 and CO releases due to Zr oxidation than what was observed in the experiments are expected.

7.2.3.6. **TOLBIAC-ICB**

The gas release coming from the decomposition of the concrete is regarded only as subsequent effect of (2D) concrete ablation. It depends on the models for metal oxidation.

7.2.4. **Oxidation reactions**

7.2.4.1. **CORQUENCH**

CORQUENCH is able to calculate gas phase chemical reactions between metallic melt constituents and sparging concrete decomposition gases (CO2 and H2O), as well as condensed phase reactions between metallic constituents and concrete decomposition products (i.e. Zr and SiO2). Tests for which CORQUENCH has been applied that included metals oxidation as well as instrumentation to detect gas speciation included ACE tests L2, L6, and L8; MACE test M1b, and SURC tests 1 and 2; see Table 4.3-2. The melt temperature predictions that include the effect of exothermic metal oxidation reactions have been compared with test data (see 7.2.2.2), but at this point gas composition has not been compared with the results of these experiments. This shortcoming will be addressed in future work.

7.2.4.2. **COSACO**

In COSACO, the thermo-chemical equilibrium in the melt pool layer based on the minimisation of the Gibbs enthalpy is calculated during each time step. Therefore, all possible chemical reactions and phase transitions are considered. This includes especially the oxidation reactions as a source of burnable gases released into the containment.

7.2.4.3. **MAAP**

Oxidation reactions are calculated by the MAAP chemical reaction model. However, the compositions calculated by MAAP have not been compared with experiments.

7.2.4.4. **MEDICIS**

The gas release which comes from the decomposition of the concrete and which impacts the oxidation of metals is regarded as subsequent effect of (2D) concrete ablation. It has recently been of second order priority in the validation work by GRS and IRSN, see Section 7.2.3.4.

7.2.4.5. **SOKRAT/HEFEST**

Again those experiments, where Zr was present in the melt, are of main interest here. Those are SURC (1-4) and ACE (L2, L6, L8). Later we expect to include tests with Cr in the melt (most likely MACE series).
7.2.4.6. **TOLBIAC-ICB**

The melt composition is calculated by GEMINI considering the chemical reactions between the different species.

7.2.5. **Top flooding**

7.2.5.1. **CORQUEENCH**

CORQUEENCH has been compared with both early flooded (MACE M1b, M3b, and M4; CCI-6) as well as late-flooded (CCI-2 and -3) experiments. As part of these analyses, the code predicts crust growth by water ingestion, particle bed formation by melt eruptions, as well as the potential for crust anchoring during the experiments. The code has been compared to the melt-water heat flux data logged during these tests, as well as the debris morphology determined as part of the post-test examinations. In general, the code seems to reasonably reproduce the initial crust formation phase as well as the heat fluxes and debris configurations determined as part of the experiments. Currently, a modelling shortcoming is the ability to predict the transient nature (i.e. on vs. off) nature of melt eruptions.

7.2.5.2. **COSACO**

Currently, only a simplified heat transfer model to a water pool above the melt is included in COSACO. No code validation for top flooding has been performed.

7.2.5.3. **MAAP**

Figure 7.2-19 shows the comparisons of the heat fluxes from corium pool to water between MAAP5.02 calculations and CCI-2 and CCI-3 data:

![Figure 7.2-19: MAAP-Validation: Comparison of heat fluxes from corium to water for CCI tests](image)

For CCI-2 test, MAAP5 calculates much lower heat flux from corium to water in the first 30 minutes after water injection. This is mainly because MAAP5 lacks a sophisticated bulk cooling model to calculate the intensive heat transfer between the corium and water in the short initial transient, when water is added on top of a corium pool in an initially dry cavity. For CCI-3 test, MAAP5 calculates a more reasonable heat flux compared to the experiment. It seems CCI-3 test does not exhibit a strong bulk cooling effect like CCI-2 test, but the heat flux measurement in this test suffers from an uncertainty of the upper surface area of the corium which is quenched by water. Figure 7.2-19 shows the heat fluxes estimated with the upper and lower bounds of the top surface area. MAAP calculation is between the two heat fluxes and closer to the one estimated with the upper bound of the top surface area.
The MAAP water ingression model was developed by Epstein (Epstein, 2004) based on earlier work by Lister (Lister, 1974) on cracking solid rock. A variant of the model has been validated by Lomperski and Farmer (Lomperski & Farmer, 2007) to compare against measurements obtained from the SSWICS tests. The model has successfully predicted the levelling-off of the heat flux from the corium to overlying water when the concrete slag mass fraction in the corium pool exceeds a critical fraction. With adjustment of a parameter in the model, the heat fluxes can agree with the experiment measurements within the data error range. There is no consideration of possible effects which evolved gas “ventilation” might have on the crust permeability in the MAAP water ingression model. Therefore, comparisons regarding the effect of ventilation on water ingression cannot be made right now.

The melt eruption model in MAAP5.02 is based on the Ricou-Spalding entrainment correlation (Ricou & Spalding, 1961). Epstein (Epstein & Zhou, 2012) has compared the entrainment rate predicted by the Ricou-Spalding correlation with the experiments performed by Tourniaire et al. (Tourniaire B., Seiler, Bonnet, & Amblard, 2006) using simulant material. According to Epstein, an entrainment coefficient of 0.08 generates an entrainment rate comparable with the Tourniaire experiment for thin crust cases. There has been no consideration in MAAP on the density (number concentration) of melt eruption sites in the upper crust. Therefore comparisons regarding the density of the eruption sites cannot be made right now.

7.2.5.4. MEDICIS

After onset of quenching, cooling mechanisms are described in the present version of ASTEC V2.0/MEDICIS by very simple models of water ingression and melt eruption:

Water ingression is described using a simple model function of an imposed permeability value; this model does not depend on the concrete oxide fraction in corium and thus does not reproduce the higher heat flux extracted in the early MCCI phase at low concrete fraction as observed in SSWICS tests (Farmer, et al., A Summary of Findings from the Melt Coolability and Concrete Interaction (MCCI) Program, 2007); in the longer term phase, water ingression is much less efficient and becomes almost negligible compared to the impact of melt eruption, so that the crudeness of the model has limited consequences on quenching predictions;

Melt eruption is described using derived from that of an older CORQUENCH version (Farmer, 2001) based on Ricou’s correlation (Ricou & Spalding, 1961) for the evaluation of the corium entrainment rate. A proportionality factor of Ricou’s correlation equal to around 0.08 permits to reproduce roughly the averaged corium entrainment rate in MACE3B and CCI2 tests.

Recently, an additional model to account for a simplified treatment of the top flooding was added to MEDICIS by GRS. This model is to be used in combination with the model options for effective heat transfer coefficients at the boundaries of the melt pool and considers a simplified top crust evolution and a boiling heat transfer at the interface between water and the top crust. The model was successfully validated for MACE M3b and M4 as well as for CCI2, -3 and -6. It was shown that assuming solidus as interface temperature between melt and top crust the experimental data (heat fluxes to the water) for these experiments can be closely approximated with a heat transfer coefficient between melt and crust of about 200 W/(m2 K) or larger, which is of similar magnitude as the heat transfer coefficients found and used between melt and concrete. This similarity is coherent with the assumed interface temperatures between melt and top crust (interface temperature is defined as solidus), as the decomposition temperature of the concrete of about ~ 1 500 to 1 600 K is not far from typical solidus temperatures of the mixtures in the longer term, when some substantial amount of concrete is mixed with the melt. However, the initial period after flooding showing intense interaction between melt and water and corresponding peak heat fluxes cannot be resolved adequately with this simplified model.
7.2.5.5. **SOKRAT/HEFEST**

It is planned to use the SWISS-2 and COMET-L3 experiments to verify the effect of top flooding, especially to check heat removal coefficients that are proposed to be used in simulations. Later it is planned to consider also the experiments from the MACE series.

7.2.5.6. **TOLBIAC-ICB**

In case of water aspersion, the upper crust surface temperature becomes lower than the concrete fusion temperature and the reactor vessel fusion temperature and then no ablation occurs above the melt level. Both radiation and convection with water are taken into account. The convective heat transfer coefficient is derived from the boiling curve of water.

Melt ejection models have been implemented in the code. The results of these models are in good agreement with the results of MACE3B and CCI2 tests (Tourniaire, Boulin, & Haquet, 2015).

The water ingression phenomenon is described using a model derived from CROQUENCH which depends on the concrete oxide fraction of corium. As this fraction increases, the water ingression phenomenon becomes less efficient and its impact on the heat transfer calculation is negligible compared to the melt ejection phenomenon.

7.2.6. **Top crust formation**

7.2.6.1. **CORQUENCH**

CORQUENCH contains a full set of models for calculating incipient crust formation and growth at the melt-water interface. As part of the validation process, efforts have been made to predict end-of-test crust thicknesses to that inferred during post-test examinations. In general, the code seems to reasonably estimate these crust thicknesses, but there is lingering uncertainty as to whether the crust thickness after the experiment is indicative of that present when the test was conducted.

7.2.6.2. **COSACO**

The temperature of the top of the melt surface is calculated by COSACO also including crust formation in case of phase segregation. No specific validation on this item has been performed.

7.2.6.3. **MAAP**

The top crust thickness predicted MAAP has not been compared with experimental data.

7.2.6.4. **MEDICIS**

Some calculated data were evaluated by GRS for experiments involving top flooding (see Section 7.2.5.4) and using the simplified option in MEDICIS for top surface heat transfer under flooded conditions. The final crust thicknesses calculated for the experiments are in the one-digit centimetre regime. Generally, in the experiments slightly thicker final top crusts were observed. In view of the uncertainties related with interface temperature, thermal conductivity of the crust, the ideal condition of a plane crust in comparison to the real processes and the problem of how to evaluate an experimental crust thickness at the time of power shutdown from post-test examinations the agreement is satisfactory.

7.2.6.5. **SOKRAT/HEFEST**

Since crust is treated automatically in the code, its thickness is derived from the upward heat flux, so we can estimate the accuracy of top crust predictions by comparison of heat flux obtained in simulations with one observed experimentally. Stable thin crust can transmit the same heat flux as thicker unstable one, so we focus our attention on heat transmission rather than on actual crust thickness, since not much experimental data about crust parameters is available anyway.
7.2.7. Effect of iron rebar

7.2.7.1. CORQUENCH

CORQUENCH is able to evaluate the effect of rebar on cavity ablation behaviour, either by homogenising iron into the concrete composition, or by treating discrete zones in which the iron is concentrated. However, the code has not yet been validated against this type of test as experiments of this type are just now emerging.

7.2.7.2. COSACO

Iron rebar in the concrete has recently been implemented in the COSACO code. This includes the addition of the steel components to the eroded concrete mass and an updated concrete decomposition enthalpy. No other effects as e.g. heat conduction into the concrete are modelled.

7.2.7.3. MAAP

The effect of iron rebar on chemical reactions and gas releases is simulated by the METOXA chemical reaction model in the code. However, since MAAP does not have a material stratification model, frozen metal at the bottom or at the top cannot be mechanistically simulated by the current version of the code. Therefore, no comparison on that aspect has been made.

7.2.7.4. MEDICIS

Iron rebar is considered in MEDICIS by providing specific data for the concrete in the input data set (e.g. including a mass fraction of iron in the composition of the concrete and a different latent heat of decomposition). No other impact of iron rebar is up to now considered in the models. The phenomenon is not validated since there had not been any dedicated MCCI experiments on this phenomenon in the past.

7.2.7.5. SOKRAT/HEFEST

Iron rebar can be considered either by providing its exact 2D structure (which is extremely time consuming since it requires very large meshes) or by declaring steel to be one of concrete component. The first approach can be used for small-scale experiments, but has small practical value. The verification of the second approach requires large-scale experiments, where the size of a single iron element is much less than the size of the concrete block and there should be a large number of iron elements, like in real NPP.

7.2.7.6. TOLBIAC-ICB

Iron rebar is considered in TOLBIAC-ICB by providing specific data for the concrete in the input data set. This way, a mass fraction of iron in the composition of the concrete and a different latent heat of decomposition is taken into account.

7.2.8. Stratification evolution and effect on heat flux distribution

7.2.8.1. CORQUENCH

As noted previously, CORQUENCH assumes oxide and metal phases are well mixed under all conditions, and so no explicit attempt has been made to model stratification in experiments.

7.2.8.2. COSACO

No dedicated stratification criterion is implemented in COSACO. The usage of layered or mixed melt configurations is predefined by user input.

In layered melt mode two configurations are considered. Initially the density of the core oxide rich oxidic melt layer is higher than that of the metallic layer. Thus the oxidic layer will be located below the metallic one. A layer of light oxides, mainly from the concrete eroded by the metallic melt, is calculated on the top. With the ongoing addition of light concrete oxides into the oxidic melt layer
its density decreases. When the metal layer density is reached inversion of the layering pattern is assumed. This yields a configuration where the oxide is located on top of the metallic melt layer. The top slag layer of light oxides is then dissolved in the oxidic melt.

7.2.8.3. **MAAP**

Stratification of immiscible materials has not been considered in MAAP MCCI models. Therefore, related comparisons cannot be made. The models may be subject to change in the future.

7.2.8.4. **MEDICIS**

Ablation of concrete by steel melts in a static stratified configuration is validated for several experiments from the BETA and COMET test series with an alumina-iron based melt. Reliable experimental data for a dynamic evolution of melt pool configuration (homogeneous <-> stratified) from experiments with prototypical oxide and steel melts and prototypical power distribution in oxide and metal are not available.

However the stratification criterion and the correlation used for heat transfer between oxide and metal layers might be deduced from the results of simulant experiments:

1) The criterion for the switch from homogeneous to stratified configuration is deduced from the onset of entrainment measured in BALISE experiments (Tourniaire, Seiler, & Bonnet, 2003).

2) The convective heat transfer between oxide and metal layers is evaluated from Greene’s correlation (Greene & Irvine, Heat transfer between stratified immiscible liquid layers driven by gas bubbling across the interface, 1988), which is in agreement within a factor of 5 with most experimental data with simulant melts (Werle and ABI data).

7.2.8.5. **SOKRAT/HEFEST**

Experimental data on dynamic behaviour of melt layers is unavailable now, and criteria used in SOCRAT/HEFEST code (temperature, time and component mass ratio) seem to be of little use in melt stratification during MCCI. Therefore we are not going to include this into present verification matrix. When we introduce superficial gas velocity as another criterion for melt stratification, we will consider this phenomenon in better detail.

7.2.8.6. **TOLBIAC-ICB**

Ablation of concrete by steel melts in a static stratified configuration is validated for several experiments from the BETA and COMET test series with an alumina-iron based melt. Reliable experimental data for a dynamic evolution of melt pool configuration from experiments with prototypical oxide and steel melts and prototypical power distribution in oxide and metal are not available.

7.2.9. **FP/aerosol release**

7.2.9.1. **CORQUENCH**

CORQUENCH does not possess the ability to model fission product release, and so validation against fission product release data is not possible.

7.2.9.2. **COSACO**

No fission product or aerosol release model is implemented in COSACO.

7.2.9.3. **MAAP**

Fission product release by formation of volatile compounds is predicted by the MAAP chemical reaction model. However, there is no comparison of the predicted fission product release with experiments.
7.2.9.4. **MEDICIS**

The FP release model in MEDICIS is tested vs. some experiments of the ACE test series. The uncertainties of the model for FP release are quite large. Depending on the chemical composition of the corium melt the uncertainty is in the range of approx. one or two orders of magnitude, but may be even much larger for some specific conditions (composition, temperature).

7.2.9.5. **SOKRAT/HEFEST**

The experiments of SURC and ACE series will be used in FP/aerosol release model verification. Since the FP/aerosol release rates strongly depend on the temperature of the melt which can differ significantly in the middle and near the wall due to high viscosity of the melt with silica and immiscible materials, we expect a high level of uncertainty here. Another source of uncertainty will be an inhomogeneity of FP distribution inside the real melt, which cannot be accounted for in current model.

7.2.9.6. **TOLBIAC-ICB**

No FP/aerosol release model is taken into account in TOLBIAC-ICB.

7.2.10. **Incubation period**

7.2.10.1. **COSACO**

No dedicated modelling is currently implemented in the COSACO code.

7.2.10.2. **MAAP**

The model in MAAP5.01 assumes crust will always be formed between the molten pool and the adjacent concrete. Crust growth and shrinkage is based on an energy balance in the entire thickness of the crust. When the corium is first relocated from the vessel, contact with the relatively cold concrete will immediately start the growth of a crust. However the crust model in MAAP assumes there is always decay heat generation in the crust. The incubation period observed in certain experiments is not modelled by the current MAAP model, since the crusts in the experiments are free of heat generation.

7.2.10.3. **MEDICIS**

The phenomena observed during the incubation period in several 2D MCCI tests with oxidic melt are referred to transient processes acting at the melt concrete interface (transient formation and re-melting of crusts eventually accompanied by transient inhomogeneous heating conditions). For such transient processes no adequate models are available in MEDICIS.

7.2.10.4. **SOKRAT/HEFEST**

The crusts that appear when the melt touches cold concrete are treated automatically in SOCRAT/HEFEST code if they are thicker than one cell of the mesh. Their dynamics can be evaluated in experiments by a change in the heat flux, but it seems quite unreliable, therefore this phenomenon is not of primary interest in SOCRAT/HEFEST code verification.

7.2.10.5. **TOLBIAC-ICB**

During the incubation period during several 2D MCCI tests with oxidic melts, several transient processes take place. Such transient processes like inhomogeneous heating of the melt are not considered in TOLBIAC-ICB.

7.2.11. **Remaining uncertainties**

7.2.11.1. **COCO**

In the COCO code, the melt is assumed to be a Newtonian fluid and the heat transfer rate is calculated with this assumption. However, the mixture of corium and concrete shows the characteristic of
Bingham plastic on a certain condition. When the behaviour of Non-Newtonian fluid affects the heat transfer between the melt and concrete, the ablation model of COCO would not be appropriate.

### 7.2.11.2. CORCON

MELCOR is considered a state-of-the-art code for severe accident modelling and analysis, and it has reached a reasonably high level of maturity over the years as evidenced by its wide acceptance and utilisation over a broad range of applications in regulatory decision support. Nevertheless, it is important to recognise the phenomenological uncertainties in MELCOR and their significance to MELCOR results. Moreover, it is important to understand the compounding effect of various uncertainties on the ultimate parameter of interest i.e., source term for all practical purposes. Some of the more important uncertainties are briefly discussed in this section.

Generally, in the case of dry cavity and metallic melts, CORCON-Mod3 thermal-hydraulic calculations were found to be reasonably in good agreement with the test data. For oxidic melts in a dry cavity, uncertainties in heat transfer models played an important role for two melt configurations – a stratified geometry with segregated metal and oxide layers, and a heterogeneous mixture. Some discrepancies in the gas release estimates were noted in a few cases. These discrepancies were attributed, in part, to condensed phase chemical reactions modelling and, in part, to experimental uncertainties. In the case of wet cavity, good agreement was found between the experimental data and code calculations except, again, for the gas release data.

The CORCON-Mod3 assessment against SWISS and MACE experiments indicate that the crust model in the code may often lead to sudden appearance and disappearance of crusts in a single calculation time step (very small time). This crust instability is an artefact of the model employed in the code. Results of the code validation exercise made it evident that differences exist between the experimental data and the code predictions. Gas release predictions for individual species show improvements when three-dimensional temperature profiles are accounted for. The combined gas release prediction is not affected as such. This suggests that for integral plant calculations, further modifications of CORCON-Mod3 in this area are not warranted.

The validation exercise revealed that the CORCON-Mod3 model dealing with chemical reactions in the metallic melt is important with regard to the code's thermal-hydraulic capability. Specifically, oxidation reactions with two metallic components – zirconium and silicon – are important. Silicon appears in the melt as a result of condensed phase reactions between zirconium and silica. The chemical heat release resulting from these reactions is exothermic at low temperatures, and endothermic at high temperatures when SiO(g) is formed. Using thermodynamic data bases, the temperature at which the reaction changes from exothermic to endothermic is estimated to be about 2350 K. The assessment provides information concerning a possible range of uncertainties in calculations for tests where temperature exceeds the above value. While in plant calculations involving siliceous concrete interacting with the core debris consideration of the SiO chemistry model will improve the gas release prediction, overall improvement in thermal-hydraulic and fission product prediction over the entire duration of core-concrete interactions is not likely to be significant.

### 7.2.11.3. CORQUENCH

CORQUENCH has been exercised against a variety of experiments under both wet and dry cavity conditions. When assessing these results as a whole, the code seems to reasonably predict melt temperature and overall cavity erosion behaviour for dry cavity cases, and also seems to reasonably predict melt temperature trends observed in tests. However, as the model currently stands, it does not do well insofar as predicting lateral/axial power splits for different concrete types (i.e. differences between limestone and siliceous concretes). In addition, the code is not able to assess potential stratification issues associated with core melts that contain a significant fraction of structural steel. Finally, the code has not been validated against tests with concrete that contain a significant amount of rebar. Regarding these last two areas (i.e. rebar and high metal content melts), there are currently
experimental programs that are providing data that will help reduce modelling uncertainties. In terms of debris coolability, it is important to develop models that can rationalise the transient nature (i.e. on vs. off) nature of melt eruptions.

7.2.11.4. COSACO

One central remaining uncertainty is the extrapolation of test results to reactor-scale. The thermo-hydraulic phenomena driving pool convection and concrete ablation might exhibit a scale-dependency which is not captured in the models since the investigation of large-scale MCCI is not practically feasible.

Another uncertainty relates to the stratification of the oxidic and metallic melt components in the pool. For large melt pools, as they would occur at plant scale, layered melt configurations can be expected. Due to the different melt/concrete interface conditions (no crust expected for metallic melt) the heat transfer to the concrete differs. Thus, to get a consistent description of the global lateral concrete erosion the inter-layer heat and mass transfer processes are decisive, as they influence the layer temperature and thickness.

7.2.11.5. MAAP

Efforts to validate the models in MAAP have been focused on ablation distance and average corium temperature. Uncertainties discovered through the validations are:

The code does not have a good shape model to account for the evolution of the ablation front observed in several 2D MCCI tests. This may not be a big issue when corium spreads on top of a large flat area. It may be an issue when corium is confined in a small space such as the sump in the reactor cavity.

A mechanistic convective heat transfer model might be needed for the transition between natural circulation (low gas evolution rate) and the gas-agitated heat transfer rate, and to allow separate prediction of downward and sideward heat transfer rates for input to the cavity shape model.

A sophisticated bulk cooling model is needed to calculate the intensive heat transfer between the corium pool and water during the initial transient after water injection. Although the current model in MAAP5 is conservative in terms of melt temperature and ablation rate without the sophisticated bulking cooling model, it may be overly conservative when high gas content concrete is ablated.

A material stratification model might be needed in MAAP5 to calculate certain conditions with high metallic content in the corium pool and small gas evolvement from concrete erosion.

The thermophysical property model in the code needs to be compared against detailed predictions using the NUCLEA database (Cheynet, Chaud, Chevalier, Mason, & Mignanelli, 2004) to check for any potential improvements.

7.2.11.6. MEDICIS

Regarding the stratified situation the validation of the inter-layer heat transfer is rather weak, since in available experiments layer temperatures and inter-layer heat fluxes are not measured and the erosion is dominated by the metal layer. There is still a large uncertainty on this parameter. However results obtained from simulant experiments indicate that the convective heat transfer between oxide and metal layers might be very high compared to the heat transfer from the bulk oxide layer to the lateral concrete interface. So if the pool stratification really occurs, a focusing of decay power towards the metal layer becomes very likely. Therefore the main uncertainty is the criterion of pool stratification in a realistic situation with real material.

Regarding 2D ablation, the slower axial ablation observed in case of siliceous concrete might be explained by the presence of a solid accumulation or crust built-up in the MCCI early phase increasing
the thermal resistance of the bottom interface. However the evolution of this solid accumulation in the long term phase and in presence of decay power in the reactor case is unknown.

It has been further observed, that there may be transient effects (like an initially superheated melt in contrast to quasi-steady state conditions) during which the constant heat transfer coefficients are not a good approximation. This is particularly due for the initial phase in the experiments with metal melt. The effect of such transients in the experiment on the final results is however not large.

Another important observation is that the heat transfer coefficients are tuned to the prediction of maximum erosion depths, which may occur in experiments only at some singularity. Since in the calculation homogeneous conditions are assumed, most experiments are overestimated with regard to the integral eroded volume but are met satisfactorily with regard to maximum erosion depths. Depending on the objective of the MEDICIS calculation the user should be aware of this “maximum erosion depth”-“integral erosion volume”-mismatch.

Generally it is not clear, which physical mechanisms dominate the heat transfer in the melt and why the efficiency of this mechanism seems constant for a long time of the experiments. Recent investigations on the effect of viscosity suggest, that the heat transfer coefficients should decrease at later times for concrete fractions in the melt > 60 wt.-%. For a verification of this hypothesis there are not enough experimental data available. A need for additional experimental data for MCCI in the late phase, with large concrete fractions and low specific heat fluxes is thus identified.

As far as corium quenching is concerned, very large uncertainties remain on the quenching efficiency in case of top quenching, in particular on the efficiency of melt eruption: the model used in MEDICIS is derived from that of an older CORQUENCH version (Farmer, 2001) and is based on a very simple correlation assuming that the entrained corium mass rate is proportional to the gas rate leaving the pool interface and is not depending on the hole geometry; these two points are clearly open to critics. Moreover the intermittent nature of melt eruption is accounted for even by more recent models as those introduced in the recent versions of CORQUENCH code (Farmer, 2001).

7.2.11.7. TOLBIAC-ICB

The validation results show a good estimation of the ablation rate but an over estimation of the melt temperature (Spindler, Tourniaire, Vandroux, Seiler, & Gubaidullin, 2005). Generally speaking, large uncertainties exist in the MCCI experiments due to the high temperature with prototypical materials. Their simulation is not easy, since an interpretation of the test conditions is necessary. As illustrated by ACE-L2 simulation, the melt temperature as calculated in TOLBIAC-ICB is very sensitive to the melt composition. This latter depends on the way corium is heated and melted, and on peculiar phenomena which can occur during the experiments (crust formation, splattering, ceramic dissolution, etc.). A good prediction of the melt composition thus requires a precise simulation of these phenomena. The crust thickness on the concrete wall, for example, is probably underestimated in the code since the cooling effects of the decomposition products of concrete which cross the crusts are not taken into account. The thermodynamic data base, which is used by GEMINI2 to obtain the liquidus temperature, may also induce uncertainty in the calculation. At last, it should be noted that the order of magnitude of the experimental uncertainty on temperature is 50 K.

Regarding 2D ablation, in case of siliceous concrete, the erosion rates are anisotropic. This is not a well-known phenomenon, CLARA experiments should provide models explaining it (more representative than a ratio between heat transfer coefficients defined by the code user). In case of siliceous concrete, the interface temperature between the crust and the melt seems to be lower than the liquidus temperature. This phenomenon is not well represented by TOLBIAC-ICB. Further investigations are needed.

Gas production has a great importance concerning the melt ejection phenomenon. In case of a siliceous concrete, it is important to consider which gas has to be considered (H2O or H2 which
depends on metal oxidation). The models based on chemical reactions deduced from pseudo-binary diagrams need to be assessed.

In case of water aspersion, the melt could freeze very quickly. The interaction between a frozen melt and the concrete is not well-known. It seems that in TOLBIAC-ICB the erosion algorithm implemented privileges the axial erosion.

The water-ingression modelling is not satisfactory in case of metal-oxide corium. No experimental data are available in this case.

7.2.11.8. SOKRAT/HEFEST

The primary source of uncertainties in 2D MCCI simulation is the applicability of BALI correlations for melt pools typical for MCCI with a flat melt layer and a strong bubbling. Errors in evaluating boundary heat flux distribution especially in a stratified melt lead to wrong prediction of concrete erosion rate, which governs wide range of processes, including gas release, FP release, etc.

7.2.11.9. WECHSL

The current level of agreement with experimental data from the ACE, MACE, SURC, BETA, COMET-L, VULCANO, MOCKA, CCI test series would appear to be good enough to justify the use of WECHSL for risk assessment studies, provided the uncertainties in the predicted results are taken into account. Nevertheless, plant calculations still require extrapolation beyond the existing experimental database. In particular the treatment of long term radial ablation by the oxide corium fraction layered over the metallic melt fraction is not yet well established, and is expected to affect the prediction for axial ablation by the metal melt.

7.3. Appendix on SEABOR crust test

In the accident of TEPCO’s Fukushima Daiichi Nuclear Power station, seawater was injected into the RPV for extended period due to loss of the freshwater source. In the present accident management in Japanese commercial light water reactors, seawater is regarded as the alternative water source. Under long-term injection of seawater, concentration of salt increases as coolant evaporates in the core and precipitates will deposit in narrow gaps in fuel bundles. When the debris bed is formed in the lower plenum or the cavity and overlaid with seawater, precipitates are produced in the particulate debris and crust surrounding the molten debris. It is anticipated that localised precipitation clogs flow paths and may degrade the heat removal significantly. Highly concentrated boric acid may co-exist under severe accident conditions in order to prevent re-criticality. It is also desirable to gain knowledge of thermophysical properties of precipitates produced from mixture of seawater and boric acid.

The molten core-concrete interaction (MCCI) of a piled debris bed on the cavity floor is dominated based on a balance among the decay heat generated from radioactive nuclides, the heat transfer to overlying pool water and the heat transfer across molten core-concrete interfaces. When submerged in a pool, surfaces of molten debris are covered with crust that is mainly composed of oxide materials. Although structure of crust is very complex in nature, its permeability and thermal conductivity are relatively low in a base matrix. Based on past experimental knowledge on MCCI, it is interpreted that the heat transfer across crust is enhanced by localised heat transfer mechanisms of water ingestion through cracks.

Salt precipitation decreases permeability in a crust base matrix and increases flow resistance of cracks, which may degrade the heat removal to overlying pool water across the upper crust and eventually enhance concrete ablation. In order to deepen understanding of this mechanism and obtaining the experimental database to establish physical models, the Regulatory Standard and Research Department, Secretariat of Nuclear Regulation Authority (S/NRA/R) planned the debris crust heat transfer test under salt precipitation as a part of the Seawater – Boric Acid Injection Heat
Removal Test (SEABOR Test). The test consists of two categories, the mockup fuel bundle test (SEABOR Bundle Test) and the debris crust test (SEABOR Crust Test).

7.3.1. Test Facility

Figure 7.3-1 shows a layout of the SEABOR Crust Test facility. Figure 7.3-2 shows a detailed plan of the test section water tank and the heat transfer surface. A simulated crust block made of oxide porous media is set with exposing its upper surface to be flush with the bottom surface of the tank. Preconditioned solutions, deionised water, artificial seawater, or mixed solution of artificial seawater and boric acid, are replenished so that the water level is maintained within a specified range. The crust is heated uniformly at the bottom surface by the copper heat transfer surface consisting of the top plate, the bottom block and ten sheath heaters. The copper heat transfer surface and the crust block are thermally connected via the thin SUS304 board (100 microns in thickness). Ceramic insulators to suppress an amount of heat loss in the lateral direction surround the crust block.
The copper top plate and the thin SUS304 board are connected with diffusion welding. The crust block is connected onto the SUS304 board with a bonding agent made of fire-proof ceramic and inorganic polymer (Aron Ceramic W of Toa Gosei). This thin SUS304 board also works as a seal to separate the water region from the copper top plate. This copper top plate is attached to the copper block with a joint bolt. Four sheath type thermocouples of the diameter 0.25mm (Okazaki-Seisakusho SUPER COUPLE K) are inserted into holes of the diameter 0.3mm drilled along the centreline of the copper top plate at the pitch 2mm. Temperature and heat flux on the surface are evaluated based on temperature signals and the linear auto-regression method. The maximum measurable temperature is 900 Kelvin.

The makeup water system consists of the water tank and the pump. Three kinds of working fluids are used, deionised water, 2.5 times concentrated artificial water or mixed solution of artificial seawater and boric acid at the same weight concentration as the artificial seawater. The water level is maintained by controlling replenishment of solutions by positioning the ball valve at the tank inlet. In making mixing fluids, artificial seawater and boric acid are stirred and prepared at planned concentration levels in different tanks. Two solutions are mixed together in the makeup water tank and heated at about 95 Celsius. Preconditioned solutions are supplied to the test section water tank by the makeup pump. The bypass line is attached to re-circulate solutions back to the makeup water tank if necessary.

7.3.2. Test Specimens

Figure 7.3-3 illustrates the simulated crust block made of oxide porous media employed in this test. The entire crust block is enveloped by a cuboid of 50mm×50mm×10mm. A dimensional tolerance is smaller than 0.5mm. The crust block is composed of small pieces that are arranged with a fixed gap distance, 2mm, in order to realise three gap patterns, no gap, number-sign shaped gap and square shaped gap. As shown in Table 7.3-1, a combination of two oxide materials, Silica and Alumina, and four different particle diameters are employed as test parameters.

<table>
<thead>
<tr>
<th>Material IDs</th>
<th>Mat-A</th>
<th>Mat-B</th>
<th>Mat-C</th>
<th>Mat-D</th>
<th>Mat-E</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material IDs</td>
<td>Silica (SiO₂) %</td>
<td>100</td>
<td>100</td>
<td>100</td>
<td>60</td>
</tr>
<tr>
<td></td>
<td>Alumina (Al₂O₃) %</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>40</td>
</tr>
<tr>
<td>Particle diameter mm</td>
<td>0.6</td>
<td>0.3</td>
<td>0.3</td>
<td>0.3</td>
<td>0.4</td>
</tr>
<tr>
<td>Porosity %</td>
<td>26</td>
<td>32</td>
<td>19</td>
<td>36</td>
<td>32</td>
</tr>
<tr>
<td>Comment</td>
<td>Large particle</td>
<td>Large porosity</td>
<td>Small particle</td>
<td>Medium heat conduction</td>
<td>Large heat conduction</td>
</tr>
</tbody>
</table>
7.3.3. Test procedures

The test is conducted according to the following steps.

Table 7.3-2 summarises major test conditions.

1. Solutions (artificial seawater and/or Boric acid) are prepared at the specified target concentration in different tanks.
2. Solutions are moved to the makeup water tank and heated at the specified temperature.
3. Finished solution is moved to the test section water tank by the makeup pump.
4. Solution in the test section tank is heated up to the saturated condition by the external heaters.
5. After reaching to the saturated condition in the test section water tank, the heaters embedded in the copper block are put on to heat the test specimen.
6. By monitoring the water level in the test section tank, replenishment of solution is adjusted to maintain the water level within the specified range.
7. When a rapid temperature increase is observed from any of the thermocouples embedded in the copper plate, it is judged that the departure from nucleate boiling occurred and the heater power is turned off.
8. The heater power is also turned off if the monitored test condition enters a risky zone, such as measured temperature values approaching the melting point of copper.

7.3.4. Test results

Influences of gap pattern, porosity and solution composition are investigated in this test. Figure 7.3-4 compares dependence of the heat flux on the wall super heating (Figure 7.3-4 (a)) and the heating time (Figure 7.3-4 (b)) among three different gap patterns for Material-E made of Alumina of the particle diameter 0.4mm submerged in solution of 2.5 times concentrated artificial seawater.
### Table 7.3-2: Major test conditions

<table>
<thead>
<tr>
<th>No.</th>
<th>Item</th>
<th>Setpoint</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Solutions composition</td>
<td>Water</td>
<td>Deionised water</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Seawater</td>
<td>Artificial seawater 2.5 time concentrated</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Mixed solution</td>
<td>Artificial seawater 1.25 times + Boric acid at the same weight concentration as seawater</td>
</tr>
<tr>
<td>2</td>
<td>Temperature of supplied solution</td>
<td>95 Celsius</td>
<td>Preheating by in the makeup water tank (Max. 1kW)</td>
</tr>
<tr>
<td>3</td>
<td>Volume of water reserved in the test section tank</td>
<td>14 L</td>
<td>210mm×210 mm×320 mm (height)</td>
</tr>
<tr>
<td>4</td>
<td>Temperature of reserved water</td>
<td>100 Celsius</td>
<td>Saturation condition is maintained in the test section tank</td>
</tr>
<tr>
<td>5</td>
<td>Power of the underlying heaters</td>
<td>Shown on the right</td>
<td>0.9 kW × 10 leaders Controlled at three levels: 2.6, 5.0, 7.0 kW</td>
</tr>
<tr>
<td>6</td>
<td>Criteria of solution replenishment</td>
<td>Target water level</td>
<td>300 mm Varies due to evaporation and supply</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Replenishment quantity</td>
<td>About 1.7 L at one shot Start when the water level becomes lower than 280 mm Stop when the water level exceeds 320 mm</td>
</tr>
<tr>
<td>7</td>
<td>Criteria of test termination</td>
<td>Occurrence of critical heat flux</td>
<td>Sudden increase of the copper base temperature</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Heater failure or the copper base melting</td>
<td>Heater : 950 Celsius Copper base : 900 Celsius</td>
</tr>
</tbody>
</table>

In a log-log plane that employs the heating time as the abscissa, the heat flux increases approximately linearly with regard to the wall super heating in case of the no gap pattern. In this case, the melting point of the copper block comes before the departure from nucleate boiling and the test is terminated at this point. The two gap patterns show higher heat removal than does the no-gap pattern and the number sign shaped pattern shows higher heat removal than does the square shaped pattern. From three boiling curves, it is interpreted that the heat transfer is enhanced as the total gap area becomes larger. The gap works as a flow path through which the coolant water is directly supplied over the heated surface without passing through the base matrix that possesses the much larger flow resistance. At the same time, generated bubbles can move smoothly through gaps and remove generated heat from the heated surface without passing through the base matrix.

By increasing the wall superheating, a sudden decrease in the heat flux is observed at about 400K and 500K for the square shaped pattern and the number sign shaped pattern, respectively. Beyond this point, two gap cases approach the no-gap case. In these test conditions, salt precipitation occurs during an evaporating concentration process. It is possible that precipitations deposited in gaps induce clogging or higher flow resistance of gaps and results in noticeable degradation of the heat transfer. The counter current flow limitation in gaps can also be attributed to these trends. Further investigations are planned to clarify underlying mechanisms.

In the three gap patterns shown in Figure 7.3-4, heating time varies from 140 min to 210 min. When employing heating time, which can be regarded as an index of accumulation of precipitations, as the abscissa, similar trends can be observed.
(a) Heat Flux vs Wall Super Heating (b) Heat Flux vs Heating Time

Figure 7.3-4: Dependence of measured heat flux on gap geometry
8. References


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State-of-the-Art Report on Molten Corium Concrete Interaction and Ex-Vessel Molten Core Coolability

Activities carried out over the last three decades in relation to core-concrete interactions and melt coolability, as well as related containment failure modes, have significantly increased the level of understanding in this area. In a severe accident with little or no cooling of the reactor core, the residual decay heat in the fuel can cause the core materials to melt. One of the challenges in such cases is to determine the consequences of molten core materials causing a failure of the reactor pressure vessel. Molten corium will interact, for example, with structural concrete below the vessel. The reaction between corium and concrete, commonly referred to as MCCI (molten core concrete interaction), can be extensive and can release combustible gases. The cooling behaviour of ex-vessel melts through sprays or flooding is also complex. This report summarises the current state of the art on MCCI and melt coolability, and thus should be useful to specialists seeking to predict the consequences of severe accidents, to model developers for severe-accident computer codes and to designers of mitigation measures.