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# TRANSIENT THERMAL-HYDRAULIC ASSESSMENT OF THE DIVERTOR PRIMARY HEAT TRANSPORT SYSTEM UNDER UP-SET AND ACCIDENTAL CONDITIONS

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# Contents

|                                                                                                           |             |
|-----------------------------------------------------------------------------------------------------------|-------------|
| <b>Disclaimer</b>                                                                                         | <b>vi</b>   |
| <b>List of Figures</b>                                                                                    | <b>vii</b>  |
| <b>List of Tables</b>                                                                                     | <b>xii</b>  |
| <b>Acronyms</b>                                                                                           | <b>xiv</b>  |
| <b>Abstract</b>                                                                                           | <b>xvii</b> |
| <b>Introduction</b>                                                                                       | <b>1</b>    |
| <b>1 The Divertor Plasma Facing Component Primary Heat Transport System of the EU-DEMO fusion reactor</b> | <b>4</b>    |
| 1.1 Key points on nuclear fusion . . . . .                                                                | 4           |
| 1.2 The EU-Demonstration fusion power plant . . . . .                                                     | 7           |
| 1.3 The DEMO Divertor . . . . .                                                                           | 11          |
| 1.3.1 The Divertor normal operation power loads . . . . .                                                 | 13          |
| 1.3.2 General description of the Divertor design . . . . .                                                | 15          |
| 1.3.3 The in-vessel Plasma Facing Components cooling circuit . . .                                        | 17          |
| 1.3.4 Challenges and Requirements of the Divertor . . . . .                                               | 18          |
| 1.4 The Primary Heat Transport System . . . . .                                                           | 21          |
| 1.4.1 The evolution of Divertor Primary Heat Transport Systems . .                                        | 22          |
| 1.4.2 Technical overview of the Plasma Facing Components Primary Heat Transport System . . . . .          | 23          |
| 1.4.3 Control systems in the Plasma Facing Components Primary Heat Transport System . . . . .             | 25          |
| 1.5 Transient accident scenarios: critical cooling failures in Divertor circuit . . . . .                 | 28          |
| 1.6 The Vacuum Vessel and the related Safety Systems . . . . .                                            | 29          |
| <b>2 Theoretical and Conceptual Framework for the Nuclear Fusion Thermal-Hydraulic Modelling</b>          | <b>32</b>   |
| 2.1 The use of Thermal-Hydraulic system code in the field of nuclear energy                               | 33          |
| 2.2 Architecture of Thermal-Hydraulic system code . . . . .                                               | 34          |

|          |                                                                                                                       |           |
|----------|-----------------------------------------------------------------------------------------------------------------------|-----------|
| 2.3      | TRACE overview . . . . .                                                                                              | 35        |
| 2.3.1    | TRACE theory - Balance and Constitutive Equations . . . . .                                                           | 36        |
| 2.3.2    | Flow regime of two-phase flow involved in TRACE . . . . .                                                             | 38        |
| 2.3.3    | Closure Relations in TRACE . . . . .                                                                                  | 40        |
| 2.3.4    | Control Procedure in TRACE . . . . .                                                                                  | 41        |
| 2.3.5    | Overview of implemented structural materials . . . . .                                                                | 41        |
| 2.3.6    | Limits of TRACE in fusion applications: the sweeping case . . . . .                                                   | 42        |
| 2.4      | Exterior Communications Interface . . . . .                                                                           | 43        |
| <b>3</b> | <b>Thermal-hydraulic modelling approach and analyses of the Plasma Facing Components under sweeping conditions</b>    | <b>46</b> |
| 3.1      | Model set-up and study of simplified generic Vertical Target . . . . .                                                | 47        |
| 3.1.1    | Thermal sub-model of simplified generic Vertical Target . . . . .                                                     | 48        |
| 3.1.1.1  | Axial discretisation of the generic Vertical Target . . . . .                                                         | 49        |
| 3.1.1.2  | Radial discretisation of the generic Vertical Target: the optimisation procedure . . . . .                            | 53        |
| 3.1.2    | Control system sub-model of simplified generic Vertical Target . . . . .                                              | 64        |
| 3.1.3    | Transient analyses and results of simplified generic Vertical Target . . . . .                                        | 66        |
| 3.2      | Model set-up and study of simplified DEMO Vertical Target . . . . .                                                   | 72        |
| 3.2.1    | Transient analyses of simplified DEMO Vertical Target . . . . .                                                       | 76        |
| 3.2.1.1  | Results for Case 1 of the Optimised Monoblock in the DEMO Vertical Target Configuration . . . . .                     | 77        |
| 3.2.1.2  | Results for Case 2 of the Optimised Monoblock in the DEMO Vertical Target Configuration . . . . .                     | 79        |
| 3.2.2    | Improvements on the DEMO Vertical Target . . . . .                                                                    | 81        |
| 3.3      | Model set-up and study of the DEMO Divertor Vertical Targets in sweeping condition . . . . .                          | 87        |
| 3.3.1    | Thermal sub-model of the DEMO Divertor Vertical Targets in sweeping condition . . . . .                               | 88        |
| 3.3.2    | Control system sub-model of the DEMO Divertor Vertical Targets in sweeping condition . . . . .                        | 90        |
| 3.3.3    | Python and ECI tools for modelling DEMO Divertor Vertical Targets in sweeping condition . . . . .                     | 91        |
| 3.3.4    | Transient analyses and results of DEMO Divertor Vertical Targets . . . . .                                            | 94        |
| 3.3.4.1  | Transient 01: Comparative Analysis of TRACE and ANSYS-CFX Under Sweeping Conditions Without Plasma Shutdown . . . . . | 95        |
| 3.3.4.2  | Transient 02: Transient Analysis under sweeping condition with a plasma shutdown in 100 s . . . . .                   | 98        |



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# List of Figures

|      |                                                                                                                                                                                      |    |
|------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|----|
| 1.1  | Illustration of confined plasma in tokamak (left) and stellarator (right) configurations . . . . .                                                                                   | 6  |
| 1.2  | EUROfusion Roadmap timeline . . . . .                                                                                                                                                | 7  |
| 1.3  | Reference duty cycle of DEMO fusion power plant . . . . .                                                                                                                            | 8  |
| 1.4  | View of the main DEMO tokamak systems . . . . .                                                                                                                                      | 10 |
| 1.5  | Conceptual scheme of the WCLL BB with EES and Heat Balance . .                                                                                                                       | 10 |
| 1.6  | Simplified depiction of the typical magnetic field structure in the edge region of a standard tokamak operating with a diverted magnetic topology (poloidal cross-section) . . . . . | 12 |
| 1.7  | Assumed breakdown of the DEMO heating power: baseline (a) and severe condition (b). . . . .                                                                                          | 14 |
| 1.8  | Heat flux breakdown in the different regions of the DIV: baseline (a) and severe condition (b). . . . .                                                                              | 14 |
| 1.9  | Comprehensive CAD model of the full DEMO DIV, presented from three different viewing perspectives . . . . .                                                                          | 16 |
| 1.10 | DIV CAD model 2019 . . . . .                                                                                                                                                         | 16 |
| 1.11 | Detailed view of an OVT PFUs assembly and half cross-section of the current DEMO monoblock, highlighting key geometrical features . . .                                              | 17 |
| 1.12 | Schematic flowchart of the PFCs in-vessel cooling circuit . . . . .                                                                                                                  | 18 |
| 1.13 | DEMO DIV PFCs in-vessel cooling circuit . . . . .                                                                                                                                    | 18 |
| 1.14 | Outer target power parallel heat flux for DEMO baseline . . . . .                                                                                                                    | 20 |
| 1.15 | Schematic drawing of sweeping the heat flux in case of loss of detachment . . . . .                                                                                                  | 21 |
| 1.16 | Overview of previous DIV PHTS . . . . .                                                                                                                                              | 22 |
| 1.17 | Concept schemes for loops arrangement . . . . .                                                                                                                                      | 22 |
| 1.18 | Overview of current DIV PHTS . . . . .                                                                                                                                               | 23 |
| 1.19 | Schematic view of a TEMA type BEU HX . . . . .                                                                                                                                       | 24 |
| 1.20 | Control system response to pressure error: heater power modulation (a) and emergency spray valve opening fraction (b). . . . .                                                       | 27 |
| 1.21 | Pressurizer level as a function of coolant average temperature . . . .                                                                                                               | 28 |
| 1.22 | Overview of a single VV sector (left) and focus on the ports (right) .                                                                                                               | 30 |

|      |                                                                                                                                 |    |
|------|---------------------------------------------------------------------------------------------------------------------------------|----|
| 1.23 | Conceptual layout of VVPSS integrated with VV considering the presence of RVs and BDs . . . . .                                 | 31 |
| 2.1  | Workflow of Problem Solving with the TRACE . . . . .                                                                            | 35 |
| 2.2  | SNAP/TRACE workflow: from model definition to simulation output                                                                 | 36 |
| 2.3  | Vertical Pre-CHF flow regimes . . . . .                                                                                         | 39 |
| 2.4  | Horizontal Pre-CHF flow regimes . . . . .                                                                                       | 39 |
| 2.5  | Schematic of inverted flow regimes in post-CHF . . . . .                                                                        | 40 |
| 2.6  | Parallel Execution of TRACE processes via ECI Driver . . . . .                                                                  | 44 |
| 2.7  | Example of taskList in ECI-based simulation . . . . .                                                                           | 45 |
| 3.1  | TRACE view of the simplified generic VT . . . . .                                                                               | 47 |
| 3.2  | Heat flux profile comparison and peak variation . . . . .                                                                       | 51 |
| 3.3  | Temporal evolution of heat flux in thermal cells for a given sweeping scenario . . . . .                                        | 52 |
| 3.4  | Axial distribution of the average heat flux over time, considering a fixed sweeping frequency of 1 Hz . . . . .                 | 52 |
| 3.5  | Definition of an equivalent thermal structure based on volume conservation . . . . .                                            | 53 |
| 3.6  | Structured workflow optimised equivalent MB . . . . .                                                                           | 55 |
| 3.7  | Geometry of the 3D-MB under investigation . . . . .                                                                             | 56 |
| 3.8  | Comparison of the temperature distribution in MB between the reference study (left) and the developed ANSYS-CFX model (right) . | 57 |
| 3.9  | Comparison heat flux and maximum temperature at W material: reference study (left) and developed ANSYS-CFX model (right) . . .  | 57 |
| 3.10 | ANSYS-CFX monitor points for defining targets under step heat flux increases . . . . .                                          | 60 |
| 3.11 | Comparison among the 3D-MB and the equivalent thermal structure                                                                 | 61 |
| 3.12 | Check on the FDM algorithm in steady-state condition . . . . .                                                                  | 63 |
| 3.13 | Set of optimum from the Pareto front . . . . .                                                                                  | 64 |
| 3.14 | Schematic overview of the control system architecture . . . . .                                                                 | 65 |
| 3.15 | Scenario 1 – thermal results . . . . .                                                                                          | 68 |
| 3.16 | Scenario 2 – thermal results . . . . .                                                                                          | 69 |
| 3.17 | Scenario 3 – thermal results . . . . .                                                                                          | 70 |
| 3.18 | Behaviour of the heat transfer coefficient . . . . .                                                                            | 71 |
| 3.19 | Evolution of flow regimes in all scenarios . . . . .                                                                            | 71 |
| 3.20 | Euclidean norm of Pareto-optimal solutions for the equivalent thermal structure of the DEMO MB . . . . .                        | 74 |
| 3.21 | Schematic view of TRACE interacting with Python Scripts by the use of ECI . . . . .                                             | 75 |

|                                                                                                                                                                                        |    |
|----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|----|
| 3.22 Thermal behaviour Case 1 scenario 1: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d). . . . .    | 77 |
| 3.23 Thermal behaviour Case 1 scenario 2: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d). . . . .    | 78 |
| 3.24 Thermal behaviour Case 1 scenario 3: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d). . . . .    | 78 |
| 3.25 Thermal behaviour Case 2 scenario 1: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d). . . . .    | 80 |
| 3.26 Thermal behaviour Case 2 scenario 2: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d). . . . .    | 80 |
| 3.27 Thermal behaviour Case 2 scenario 3: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d). . . . .    | 81 |
| 3.28 Sketch of the analytical geometry . . . . .                                                                                                                                       | 82 |
| 3.29 View of the half surface participating in the heat exchange . . . . .                                                                                                             | 82 |
| 3.30 Flow chart for training the regression model . . . . .                                                                                                                            | 83 |
| 3.31 Procedure for correcting tungsten temperature by the use of the linear regression model . . . . .                                                                                 | 84 |
| 3.32 Thermal behaviour of the analytical geometry scenario 1: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . . | 85 |
| 3.33 Thermal behaviour of the analytical geometry scenario 2: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . . | 86 |
| 3.34 Thermal behaviour of the analytical geometry scenario 3: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . . | 86 |
| 3.35 SNAP view of the single PFCs in-vessel cooling circuit . . . . .                                                                                                                  | 88 |
| 3.36 Sketch of the thermal structure of the back side of PFU . . . . .                                                                                                                 | 89 |
| 3.37 Sketch of the types of thermal structures implemented in TRACE model of PFC cooling circuit . . . . .                                                                             | 89 |
| 3.38 Schematic overview of the control system architecture to model volumetric loads . . . . .                                                                                         | 91 |
| 3.39 Time evolution of the Eich Profile on the TRACE model of the OVT without sweeping . . . . .                                                                                       | 93 |

|      |                                                                                                                                                                                                                                                                                                                  |     |
|------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-----|
| 3.40 | Axial distribution of the average heat flux over time in OVT considering the sweeping of the Eich profile . . . . .                                                                                                                                                                                              | 94  |
| 3.41 | Time evolution of the thermal load on the outer surface of the ANSYS-CFX W domain, considering the sweeping of the Eich profile (orange curve), and the time-averaged value of the applied load (blue curve): Region 01 - extremes of the sweeping amplitude (a), Region 02 -initial position of SP (b). . . . . | 96  |
| 3.42 | Sweeping of Eich profile without shutdown Region 01: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . .                                                                                                                                    | 97  |
| 3.43 | Sweeping of Eich profile without shutdown Region 02: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . .                                                                                                                                    | 97  |
| 3.44 | Sweeping of Eich profile Transient 02 Region 01: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . .                                                                                                                                        | 99  |
| 3.45 | Sweeping of Eich profile Transient 02 Region 02: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . .                                                                                                                                        | 100 |
| 3.46 | Sweeping of Eich profile Transient 03 Region 01: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . .                                                                                                                                        | 101 |
| 3.47 | Sweeping of Eich profile Transient 03 Region 02: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature. . . . .                                                                                                                                        | 101 |
| 4.1  | SNAP view of the TRACE model of the DIV PFCs PHTS . . . . .                                                                                                                                                                                                                                                      | 106 |
| 4.2  | SNAP view of the TRACE model of the PRZ . . . . .                                                                                                                                                                                                                                                                | 107 |
| 4.3  | SNAP view of the TRACE model of the U-tube HX and the pump . . . . .                                                                                                                                                                                                                                             | 107 |
| 4.4  | Pump coast-down profile . . . . .                                                                                                                                                                                                                                                                                | 108 |
| 4.5  | TRACE model details: focus on VV (a) and VVPSS (b). . . . .                                                                                                                                                                                                                                                      | 109 |
| 4.6  | HTC profile related to FWall and SW . . . . .                                                                                                                                                                                                                                                                    | 110 |
| 4.7  | Signals location for the reactor trip . . . . .                                                                                                                                                                                                                                                                  | 111 |
| 4.8  | Cold feeding pipe DEG break location . . . . .                                                                                                                                                                                                                                                                   | 112 |
| 4.9  | VV fundamental parameters during in-vessel LOCA: sequence of valves opening over time leading to VVPSS (a), time-evolution of pressure of VV (b), MFR across the RVs of VVPSS (c) and accumulated coolant in the collection tank and VV(d). . . . .                                                              | 113 |

|      |                                                                                                                                                                                                                                               |     |
|------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-----|
| 4.10 | PHTS fundamental parameters during in-vessel LOCA: loop pressures time-evolution (a), time-evolution of mass trend in the loop (b), MFR across the DEG rupture (c) and time-evolution of temperature of the PFU unit (d). . . . .             | 114 |
| 4.11 | Pressure trends of VV and PHTS . . . . .                                                                                                                                                                                                      | 115 |
| 4.12 | Cold leg DEG break location . . . . .                                                                                                                                                                                                         | 117 |
| 4.13 | Ex-vessel LOCA transient A: reactor trip initiation. . . . .                                                                                                                                                                                  | 118 |
| 4.14 | Time evolution of CCZ maximum temperature during ex-vessel LOCA considering a plasma shutdown in: 50 (a), 100 (b), 150 (c) and 200 seconds (d). . . . .                                                                                       | 118 |
| 4.15 | PHTS fundamental parameters during ex-vessel LOCA transient A: pressures comparison between loop and TCR (a), time-evolution of PRZ water level (b), MFR across the DEG rupture (c) and time-evolution of mass trend in the loop (d). . . . . | 119 |
| 4.16 | Cold leg DEG break location . . . . .                                                                                                                                                                                                         | 120 |
| 4.17 | Ex-vessel LOCA transient B: reactor trip initiation. . . . .                                                                                                                                                                                  | 121 |
| 4.18 | Ex-vessel LOCA transient B: Time evolution of CCZ maximum temperature. . . . .                                                                                                                                                                | 121 |
| 4.19 | PHTS fundamental parameters during ex-vessel LOCA transient B: pressures comparison between loop and TCR (a), time-evolution of PRZ water level (b), MFR across the DEG rupture (c) and time-evolution of mass trend in the loop (d). . . . . | 122 |
| 4.20 | Swept heat flux profile in detachment condition . . . . .                                                                                                                                                                                     | 124 |

# List of Tables

|      |                                                                                                                                    |     |
|------|------------------------------------------------------------------------------------------------------------------------------------|-----|
| 1.1  | Relevant parameter of DEMO device . . . . .                                                                                        | 12  |
| 1.2  | Integral power breakdown for the Divertor (DIV) Plasma Facing<br>Components (PFCs): baseline (a) and severe condition (b). . . . . | 15  |
| 1.3  | Input thermal power and temperatures of PHTSs. . . . .                                                                             | 21  |
| 1.4  | Thermal-hydraulic parameters of the HX. . . . .                                                                                    | 24  |
| 1.5  | Pressurizer sizing summary. . . . .                                                                                                | 25  |
| 1.6  | Main coolant pumps design parameters. . . . .                                                                                      | 25  |
| 3.1  | Geometry data of the Monoblock (MB). . . . .                                                                                       | 55  |
| 3.2  | Thermal-Hydraulic (TH) input parameter. . . . .                                                                                    | 56  |
| 3.3  | Assumed thermodynamic and transport properties. . . . .                                                                            | 58  |
| 3.4  | Optimised variables of the equivalent MB. . . . .                                                                                  | 59  |
| 3.5  | Weights of the targets. . . . .                                                                                                    | 61  |
| 3.6  | Selected optimal solution for each case. . . . .                                                                                   | 64  |
| 3.7  | TRACE investigated scenarios. . . . .                                                                                              | 66  |
| 3.8  | Main output parameter of the reference study. . . . .                                                                              | 67  |
| 3.9  | Scenario 1: percentage errors compared to the case study. . . . .                                                                  | 68  |
| 3.10 | Scenario 2: percentage errors compared to the case study. . . . .                                                                  | 69  |
| 3.11 | Cumulative CPU for the simulations. . . . .                                                                                        | 72  |
| 3.12 | Target values derived from thermal simulations. . . . .                                                                            | 73  |
| 3.13 | Weights used in the optimisation procedure for Case 1 (a) and Case<br>2 (b). . . . .                                               | 73  |
| 3.14 | Assumed thermodynamic and transport properties for DEMO PFU<br>fundamental unit. . . . .                                           | 74  |
| 3.15 | Selected optimal solution for each case. . . . .                                                                                   | 74  |
| 3.16 | Cumulative machine times for the simulations. . . . .                                                                              | 76  |
| 3.17 | Cumulative machine times for the simulations. . . . .                                                                              | 84  |
| 3.18 | Percentage errors of TRACE simulations compared to the real PFU<br>fundamental unit. . . . .                                       | 87  |
| 3.19 | Percentage errors of TRACE simulations compared to ANSYS-CFX<br>considering Eich profile without plasma shutdown. . . . .          | 98  |
| 4.1  | Summary of TRACE components used in the thermal-hydraulic model.                                                                   | 105 |

|     |                                           |     |
|-----|-------------------------------------------|-----|
| 4.2 | Investigated ramp down durations. . . . . | 117 |
|-----|-------------------------------------------|-----|

# Acronyms

**BB** Breeding Blanket

**BC** Boundary Condition

**BD** Burst Disk

**CB** Cassette Body

**CHF** Critical Heat Flux

**CSOL** Central Solenoid

**Cu** Copper

**CuCrZr** Copper-Chromium-Zirconium

**CVCS** Chemical and Volume Control System

**DBA** Design Basis Accident

**DCT** DEMO Central Team

**DEG** Double-Ended Guillotine

**DEMO** DEMOnstration fusion power reactor

**DIV** Divertor

**ECI** Exterior Communications Interface

**EFDA** European Fusion Development Agreement

**ENEA** Agenzia nazionale per le nuove tecnologie, l'energia e lo sviluppo economico sostenibile

**ESF** Engineered Safety Feature

**ESS** Energy Storage System

**FDM** Finite Difference Method

**FW** Feed-Water

**FWall** First Wall

**GAMULTIOBJ** multi-objective genetic algorithm

**HTC** Heat Transfer Coefficient

**HX** Heat eXchanger

**ICE** Ingress of Coolant Event

**ITER** International Thermonuclear Experimental Reactor

**IVT** Inner Vertical Target

**LCFS** Last Closed Flux Surface

**LOCA** Loss of Coolant Accident

**MB** Monoblock

**MFR** Mass Flow Rate

**NFPP** Nuclear Fusion Power Plant

**NPP** Nuclear Fission Power Plant

**NRC** U.S. Nuclear Regulatory Commission

**OVT** Outer Vertical Target

**PA** Postulated Accident

**PCS** Power Conversion System

**PFC** Plasma Facing Component

**PFCoils** Poloidal field coils

**PFU** Plasma Facing Unit

**PHTS** Primary Heat Transport System

**PRZ** Pressurizer

**R&D** Research and Development

**RL** Relief Line

**RP** Reflector Plate

|                 |                                               |
|-----------------|-----------------------------------------------|
| <b>RV</b>       | Release Valve                                 |
| <b>SDG</b>      | Sustainable Development Goal                  |
| <b>SL</b>       | Shielding Liner                               |
| <b>SNAP</b>     | Symbolic Nuclear Analysis Package             |
| <b>SOL</b>      | Scrape-Off Layer                              |
| <b>SP</b>       | Strike Point                                  |
| <b>SRV</b>      | Safety Relief Valve                           |
| <b>ST</b>       | Swirl Tape                                    |
| <b>SW</b>       | Side Wall                                     |
| <b>SYS Code</b> | System Code                                   |
| <b>TBR</b>      | Tritium Breeding Ratio                        |
| <b>TCR</b>      | Tokamak Cooling Room                          |
| <b>TFC</b>      | Toroidal field coils                          |
| <b>TH</b>       | Thermal-Hydraulic                             |
| <b>tokamak</b>  | TOroidal'naya KAmera s MAgnaitnymi Katushkami |
| <b>TRACE</b>    | TRAC/RELAP Advanced Computational Engine      |
| <b>VS</b>       | Vertical Shaft                                |
| <b>VT</b>       | Vertical Target                               |
| <b>VV</b>       | Vacuum Vessel                                 |
| <b>VVPSS</b>    | Vacuum Vessel Pressure Suppression System     |
| <b>W</b>        | Tungsten                                      |
| <b>WCLL</b>     | Water-Cooled Lithium Lead                     |

# Abstract

Nuclear fusion has become a strategic focus in the global effort to develop sustainable and secure energy systems, offering the potential for virtually unlimited power generation with minimal carbon emissions and environmental impact. Nevertheless, the industrial deployment of fusion energy remains one of the most complex technological challenges in the energy sector.

Within this framework, the European Union has actively supported fusion research, with the DEMOnstration fusion power reactor (DEMO) project identified as a pivotal milestone in the European fusion roadmap. DEMO is intended to demonstrate the feasibility of delivering electricity to the grid from fusion, operating with a closed fuel cycle and incorporating advanced systems for plasma confinement, energy conversion, and safety management.

Among the critical subsystems within DEMO is the Divertor (DIV), responsible for power exhaust and particle control. The DIV Plasma Facing Components (PFCs) are exposed to extreme thermal loads, particularly under off-normal conditions, where heat fluxes may exceed several tens of  $\text{MW}/\text{m}^2$ . Their correct functioning under such conditions is inextricably linked to the design of the cooling circuits and, more broadly, to the performance of their Primary Heat Transport Systems (PHTSs).

To address these challenges, the research conducted during the Ph.D. has led to the development of an integrated methodology for the thermal and Thermal-Hydraulic (TH) characterisation of the PFCs and their PHTS under upset plasma transients and accidental scenarios.

The approach combines the TRAC/RELAP Advanced Computational Engine (TRACE) system code for global TH modelling, ANSYS-CFX for local analyses, optimisation algorithms and regression models for geometric simplification, and the Exterior Communications Interface (ECI) interface to couple plasma-induced thermal loads with the simplified TH model of the PFCs.

The research activity has developed along two complementary lines. The first focuses on the analysis of sweeping application on a single in vessel cooling circuit of PFCs during the loss of detachment, aiming to identify the most thermally critical regions of the DIV and to assess the effectiveness of mitigation strategies under this event. The second line of investigation addresses both in-vessel and ex-vessel

Loss of Coolant Accident (LOCA) scenarios, with the objective of characterising the response of the PHTS and evaluating the performance of safety systems such as the Vacuum Vessel Pressure Suppression System (VVPSS). These studies provide insight into the ability of PHTS and VVPSS to manage severe transients and highlight potential vulnerabilities.

The results confirm the robustness and versatility of the proposed methodology, demonstrating its capability to simulate complex transient phenomena and accurately assess the TH behaviour of the PFCs and the overall PHTS.

This modelling framework constitutes a robust and adaptable foundation for the TH design and safety assessment of the DEMO DIV. Its modular architecture and dynamic coupling capabilities enable detailed analyses of complex transients, support parametric investigations, and facilitate future extensions to other in-vessel components and Nuclear Fusion Power Plant (NFPP) configurations. As such, it provides a valuable toolset for guiding design optimisation, licensing evaluations, and integrated fusion reactor simulation strategies.

# Introduction

The pursuit of sustainable and safe energy sources has placed nuclear fusion at the forefront of global scientific research, driven by its potential to provide virtually limitless, low-carbon energy with minimal environmental impact. Unlike fossil fuels, which contribute significantly to greenhouse gas emissions and climate change, and nuclear fission, which generates long-lived radioactive waste, fusion offers a cleaner alternative by producing helium as a product and requiring fuel sources that are abundant and widely available.

This promising and innovative technology will be essential to meet the United Nations Sustainable Development Goals (SDGs) [1], aligning directly with the SDG-7 (Affordable and Clean Energy), which calls for universal access to reliable, sustainable, and modern energy services by 2030. Additionally, the adoption of fusion energy also supports SDG-13 (Climate Action) by offering a carbon-free energy source capable of significantly reducing global emissions and mitigating the impacts of climate change.

Due to the enormous technological challenges that must be overcome in order to industrially use this energy source, the generation of electricity from fusion power plants is one of the most ambitious energy projects in the world today, despite the fact that it is unquestionably of significant interest.

In recent years, the European Union has promoted research and technological development for the exploitation of nuclear fusion to produce electricity, recognising its potential benefits.

Within this framework, the first Fusion Roadmap was released in 2012 by the European Fusion Development Agreement (EFDA) [2]. This document is crucial to fusion research since it outlines the main issues and priorities that must be resolved in order to realise industrial Nuclear Fusion Power Plants (NFPPs) and establishes deadlines for reaching the objectives. After EFDA has been replaced by EUROfusion, a consortium of fusion research institutions with members from the European Union, Switzerland, Ukraine, and the United Kingdom, the Roadmap has been subsequently modified [3]. The revised version outlines a strategic pathway that begins with the technical demonstration of large-scale fusion power through International Thermonuclear Experimental Reactor (ITER), continues with the delivery of electricity to the grid via DEMONstration fusion power reactor (DEMO)

while simultaneously investigating alternative concepts such as stellarators, and culminates in the large-scale industrial production and deployment of NFPPs.

Among the fundamental steps defined by this new Roadmap, the DEMO project represents a critical milestone. The experimental device is expected to deliver hundred MW of electricity to the grid at the end of this century, operating with a closed fuel cycle and replicating many of the characteristics required for future commercial NFPP.

Due to its ambitious goals, DEMO is expected to encounter significantly more demanding challenges, both related to the fundamental components which are the basis of the TORoidal'naya KAmera s MAGnaitnymi KATushkami (tokamak) architecture and the systems to be integrated to extract fusion power and convert it into electricity production.

In particular, one of the most critical aspects of DEMO is the technological development of the Divertor (DIV), which belongs to the group of in-vessel components and is responsible for power handling and particle exhaust. In particular, the Plasma Facing Components (PFCs) DIV are in direct contact with the plasma or its exhaust and must handle considerable heat fluxes in normal operation conditions and much higher values in the order of several megawatts per square meter in off-normal conditions while ensuring both safety and efficiency.

Indeed, a key element in the design of a NFPP is the amount of power that can be reliably controlled within the system. This capability is strongly influenced by the thermal limits of the DIV and its ability to operate effectively in steady-state or long-pulse regimes. The correct functioning of the DIV under these conditions is inextricably linked to the design of its cooling circuits and, more broadly, to the performance of its Primary Heat Transport Systems (PHTSs). These systems must ensure efficient and safe heat removal, maintaining the structural and functional materials within their optimal temperature ranges.

The efficacious design of these systems is not just a technical necessity but a cornerstone of the overall DEMO viability. Therefore, early identification and resolution of potential issues related to the in-vessel component and its cooling circuits must be addressed through a multi-disciplinary approach, accounting for plasma scenarios, Thermal-Hydraulic (TH) behaviour, and structural performance, to support a robust and licensable system design.

In this context, the activity conducted during the XXXVIII Cycle of the Ph.D. Course in Energy – Low carbon Energetics and Innovative Nuclear Systems – held at the University of Palermo has been framed within DEMO conceptual design activities and has been supported by the EUROfusion consortium, of which the University of Palermo is an affiliated entity through the Agenzia nazionale per le nuove tecnologie, l'energia e lo sviluppo economico sostenibile (ENEA).

The research has been particularly focused on the development of methodologies

that allow to study the TH behaviour DEMO PFCs DIV PHTS under off-normal and accidental conditions.

The activity involved a dedicated study of both the in-vessel circuit and the PHTS as a whole, using a TH code, namely TRAC/RELAP Advanced Computational Engine (TRACE), with the aim of characterising their performance during upset operational scenarios and incidental transients. The methodology developed required a multidisciplinary approach that considered not only technological constraints, but also plasma physics aspects and circuit safety requirements.

In addition, it has been necessary to rely not only on TRACE, but also, depending on the specific modelling needs, on finite volume code (ANSYS-CFX) and various scripts developed in MATLAB and Python. In particular, Python scripting enabled the replication of specific thermal loads within the TRACE models by the coupling through of the Exterior Communications Interface (ECI) driver. This approach allowed for greater flexibility and precision in simulating complex thermal scenarios and contributed to a more comprehensive understanding of the system behaviour under different operating conditions.

Therefore, the dissertation here reported is divided as follows:

- chapter 1 presents some generalities on nuclear fusion physics, DEMO fusion device with a focus on the DIV and a general description of PHTS and related safety systems;
- chapter 2 illustrates the use of the TRACE in the fusion field, describing the governing equations, the implemented simplified models and giving a general overview of ECI, which allows to couple the developed model with dedicated Python scripts;
- chapter 3 modelling strategies and TH studies of the in-vessel PFC single cooling circuit during a sweeping transient in case of loss of detachment;
- chapter 4 TH studies of the PFCs DIV PHTS during in-vessel and ex-vessel Loss of Coolant Accident (LOCA).

# Chapter 1

## The Divertor Plasma Facing Component Primary Heat Transport System of the EU-DEMO fusion reactor

### 1.1 Key points on nuclear fusion

Nuclear fusion is the fundamental process that fuels the Sun and stars, playing a central role in the evolution of the universe. This kind of nuclear reaction involves the merging of two low atomic nuclei under proper conditions, which subsequently disintegrate, releasing reaction products with considerable kinetic energy.

A fusion reaction releases an energy quantity depending on the mass variation of the reactant nuclei. The products are stable, the mass difference between reactants and them manifests as energy according to Einstein's law  $E = \Delta m \cdot c^2$ , in which  $c$  is the speed of light.

In the case of fusion reaction between the hydrogen isotopes deuterium ( $D$ ) and tritium ( $T$ ) equation (1.1), which appears to be the most promising of those selected as fuels for future prototype and commercial NFPP, the released energy (17.6 MeV) is distributed in the form of kinetic energy between the products, namely a neutron ( $n$ ) and an alpha particle ( $\alpha$ ), respectively  $E_{k,n}=14.1$  MeV and  $E_{k,\alpha}=3.5$  MeV.



Although deuterium is abundant in natural water (0.0153 at.% in sea-water [4]), tritium is scarce and a radioactive beta emitter with a half-life of 12.3 years. For this reason, it is necessary to produce the tritium, and it is expected that the main supply of this fuel in future NFPP will be from lithium.

In particular, two key fission reactions equation (1.2) and equation (1.3) enable lithium to serve as an effective fuel in fusion plants.



The tritium breeding will occur in the Breeding Blanket (BB), which will cover most of the plasma chamber of fusion power plants. In order to be self-sustaining, for each fusion reaction, at least one tritium nucleus must be produced from the lithium fission reactions. In this respect, the key parameter is the Tritium Breeding Ratio (TBR), defined as the ratio between the produced tritium in the BB and the amount consumed in the plasma. To ensure the tritium self-sufficiency, the TBR must exceed unity and a minimum criterion foresees a  $\text{TBR} > 1.1$  to account for uncertainties in nuclear data, tritium losses during extraction, decay, and inventory build-up for startup and reserve purposes [5]. However, since the cross-section of these reactions is limited, achieving a TBR above unity requires enhancing the neutron economy. For this purpose, neutron multiplier materials such as beryllium (Be) or lead (Pb) are integrated into the BB, where they undergo (n,2n) reactions that increase neutron availability and boost overall tritium production [6].

The fusion reaction between two light nuclei occurs because the strong nuclear force (attractive) prevails over the electrostatic force (repulsive). The strong nuclear force only acts over very short distances (within a certain threshold radius  $R_0$ ), and binds protons and neutrons inside the nucleus. On the other hand, the electrostatic force, described by Coulomb's law, decreases with the square of the relative distance and can either attract or repel depending on the charges involved.

In particular, considering two nuclides of like charge ( $q_1$  and  $q_2$ ) and a mutual distance equal to the threshold  $R_0$ , the potential energy is called Coulomb barrier and it represents the minimum kinetic energy that a particle must possess to overcome electrostatic repulsion and come quite close to other in order to make nuclear force dominate.

$$U_r = \frac{1}{4\pi\epsilon_0} \cdot \frac{q_1 q_2}{R_0} \quad (1.4)$$

However, thanks to the quantum tunnelling effect, the particles penetrate the Coulomb barrier with an energy lower compared to the one calculated from equation (1.4) and the probability of this penetration depends on the relative speed of the moving particles. If the particle reaches a distance  $r < R_0$  from each other, the attractive strong nuclear force dominates over the electrostatic force and a compound nuclear state is formed.

It follows that fusion is an exoergic and thermodynamically favoured reaction

because the product nucleus is more stable than the reactants. However, it is kinematically disadvantaged since it is a threshold reaction.

Since light nuclei like deuterium and tritium have a Coulomb barrier in the range of 0.4 MeV, in order to obtain frequent reactions, it is necessary to heat particles up to a temperature for which a considerable number of nuclei possess the kinetic energy to overcome the threshold. Considering the tunnelling effect, the barrier lowers in the tens-of-keV range and the heating decreases up to the temperature near  $10^8$  K.

At this temperature range, the gas exists as a large-scale neutral collection of ions and unbound electrons, which is called plasma. In this essential configuration of matter, the alpha particles are confined, and their energy is distributed via collisions among the charged particles until they reach the plasma chamber wall. On the other hand, the produced neutrons immediately leave the plasma and transfer their energy to the surrounding plasma-facing components, generating a volumetric heat source while causing radiation damage to the materials. Furthermore, the neutrons, once they reach the lithium in the BB, can be captured and produce tritium in accordance with the fusion fuel cycle.

Plasma must be confined to guarantee a high reaction rate, thus using a high density of particles or confining them for a long time. Therefore, the most promising solutions to exploit fusion reactions are respectively the inertial and magnetic confinements [7]. The latter method has a broader scientific and technological background, supported already by several devices aimed at demonstrating its technical feasibility.

The oldest magnetic concepts foresee the adoption of a solenoidal field, however, the high particle leakage occurring at the ends of the solenoid makes this technology unattractive for NFPP. Therefore, the optimal strategy to limit the leakages has been identified as the adoption of a toroidal field configuration. This configuration is the basis of the tokamak [8] or stellarator [9] architectures (figure 1.1).

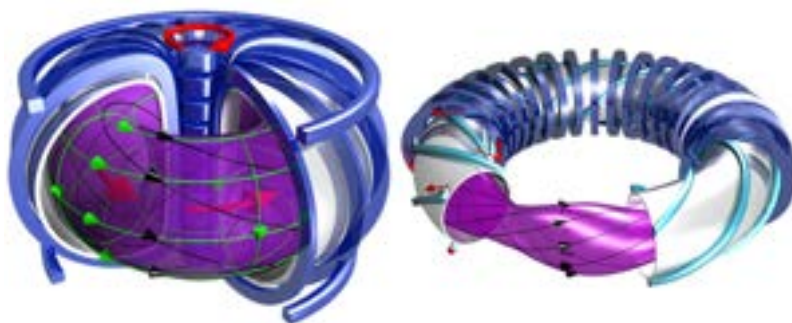


**Figure 1.1:** *Illustration of confined plasma in tokamak (left) and stellarator (right) configurations.*

Most of the fusion reactors already developed are based mainly on the tokamak configuration, and it will be adopted in the same way for the next fusion devices, such as ITER and DEMO. The dissertation presented here focuses on the tokamak concept and, more specifically, on DEMO.

## 1.2 The EU-Demonstration fusion power plant

The EUROfusion Consortium, acting on behalf of the European Commission, has developed the European Research Roadmap to the Realisation of Fusion Energy [3]. This strategic plan outlines the key scientific and technological milestones required to deliver fusion-generated electricity to the grid figure 1.2.

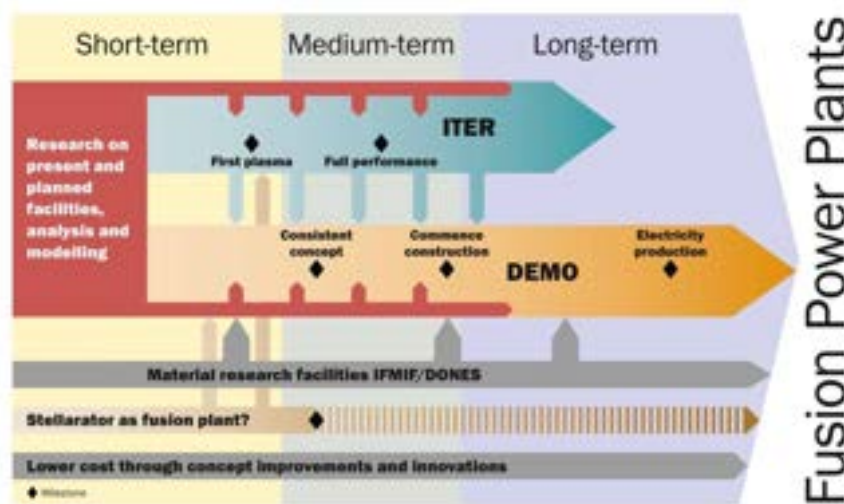


**Figure 1.2:** *EUROfusion Roadmap timeline* [3].

According to the strategy outlined in the document, DEMO will be the link between the experimental ITER and NFPPs operating on a commercial scale. Through DEMO the goals are to achieve:

- delivering 300 - 500 MW of electricity to the grid;
- demonstrating safety and environmental sustainability;
- using a closed fuel cycle;
- demonstrating the feasibility and economic viability of the plant;
- validating fusion power plant relevant technologies with the resolution of all physics and engineering issues foreseen in the plant.

Due to the ambitious goals, DEMO is expected to encounter significantly more demanding technological challenges than those already faced by ITER.

The construction and subsequent operation of DEMO will be divided into three well-structured phases, each supported by several secondary objectives. The

Pre-Conceptual Design phase, completed in 2020, aimed to advance the technical foundation of the reactor design. Special focus was placed on comparing different plant design options with a reference concept to identify the configurations with the highest likelihood of success. These selected concepts will form the foundation for future development. At the end of the optimisation process (Conceptual Design phase), only the most promising configuration will be chosen for the Engineering Design phase, where the final design will be developed [3].

Since DEMO will be based on the tokamak architecture, its operating mode will be pulsed, meaning that the plasma power output follows a cyclic pattern. The DEMO consists of two main phases, Pulse and Dwell, connected by two transitional phases, which are respectively the Ramp-up and the Ramp-down.

During the Ramp-up phase (approximately 100 – 200 s), the plasma power increases from 0 to its nominal value of  $2\text{ GW}_{\text{th}}$ . The Pulse phase lasts for 7200 s (2 h), during which the plasma maintains full power. The Ramp-down phase (again 100–200 s) brings the plasma power back to zero. Finally, the Dwell phase lasts for 600 s (10 min), during which no fusion power is generated. However, decay heat remains in the reactor structures, amounting to approximately 1 – 2% of the nominal power one second after shutdown. This residual power is not related to the plasma itself, but to the thermal energy stored in the structural materials [10].

This behaviour is illustrated in the DEMO duty cycle diagram (see figure 1.3), which shows the time evolution of plasma power throughout the cycle.

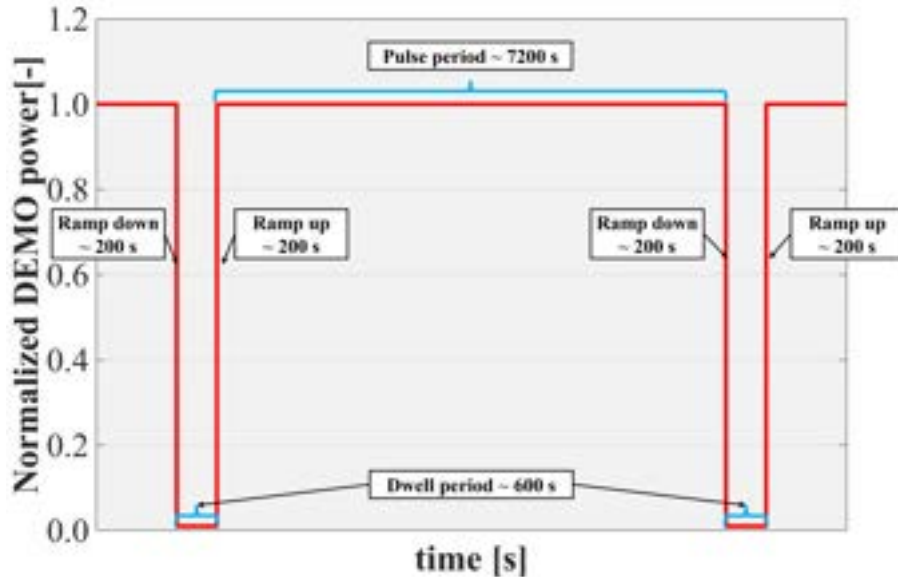


**Figure 1.3:** Reference duty cycle of DEMO NFPP [10].

Generally, the DEMO architecture is composed of the following main systems [11]:

- the Magnet System: constituted by a set of superconducting coils, actively cooled by helium at 4 K and required to confine, shape and control the plasma.

These include the Central Solenoid (CSOL), Poloidal field coils (PFCoils) and Toroidal field coils (TFC);

- the Vacuum Vessel (VV): is a torus-shaped double-walled pressure vessel. It provides the vacuum environment, shields the magnet system from neutrons and acts as mechanical support for the in-vessel components;
- the Cryostat: is a large steel structure containing the VV and superconducting magnets. It ensures a cryogenic, low-pressure environment, and it serves as a secondary containment barrier providing structural support for the superconducting magnet system and the VV;
- the BB: covers most of the VV inner surface. Its main functions are the production and extraction of tritium, the removal of the thermal power released by the fusion products and the shielding of the VV and coils;
- the DIV: is primarily a high heat flux component which collects most of the particles and energy exhausted by the plasma, and it fulfils shielding functions as well. It is located in the lower region of the VV;
- the PHTSs: are the set of piping and equipment located both in-vessel and ex-vessel, appointed to cool the components and transfer the heat from the plasma to the Power Conversion System (PCS);
- the PCS: is the secondary cooling loop where the heat removed from the tokamak by the PHTSs is transferred to spin a turbine and to ensure an efficient conversion into electrical power. Depending on the cooling strategy of the BB, it may or may not be equipped with a small Energy Storage System (ESS);
- the heating and current drive system: aims to heat and control the plasma, ensuring the high temperature;
- the supply and circulation fuel system;
- the maintenance and monitoring systems.

The figure 1.4 provides an idea of the device and the location of its main systems, while one of the conceptual schemes of the PHTSs and PCS is shown in figure 1.5. Specifically, figure 1.5 depicts the Water-Cooled Lithium Lead (WCLL) BB configuration, which stands as one of the reference concepts for tritium breeding and heat removal in DEMO. This design integrates pressurised water cooling with PbLi acting as both breeder and neutron multiplier, ensuring compatibility with established nuclear technologies. The WCLL BB configuration will serve as a key reference throughout the present dissertation.

It should therefore be noted, given the ambitious goals of the project, that the proper functioning of the in-vessel components, such as BB and DIV, is a crucial aspect to the successful realisation of DEMO. Their design is indeed challenging and requires a substantial Research and Development (R&D) and technology effort from the scientific community. In particular, due to its function and location, the DIV

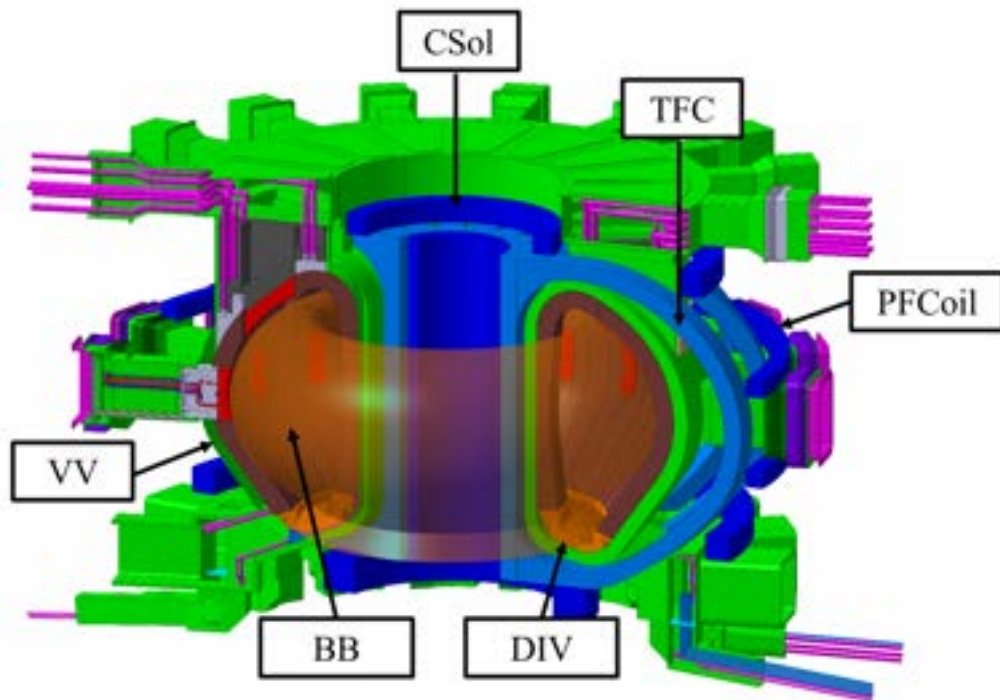


Figure 1.4: View of the main DEMO tokamak systems.

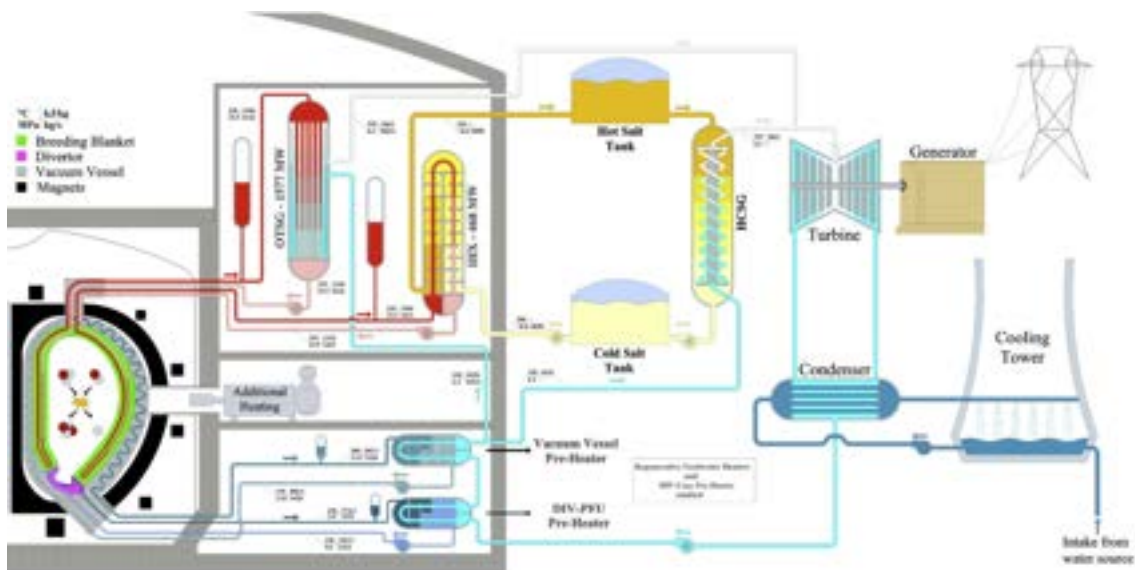


Figure 1.5: Conceptual scheme of the WCLL BB with the EES and Heat Balance [12].

will be subjected to a very harsh loading environment [13], while still being required to operate reliably throughout its expected lifetime.

In order to ensure a robust and reliable component, it must be operated under optimal conditions, and in this regard, the cooling system it interfaces with plays a fundamental role. Therefore, the criteria of reliability and robustness apply equally to both the in-vessel component and the PHTSs.

The following section provides a brief introduction to the DIV in-vessel component, with a focus on the high heat flux portion, followed by a description of the PHTS with which it interacts.

Finally, a brief description of the VV and related safety systems will be provided, which are essential for the DIV PHTSs in the case of in-vessel accident scenarios involving the release of coolant into the plasma chamber.

### 1.3 The DEMO Divertor

The DIV plays a pivotal role as an in-vessel component in the fusion reactor carrying out challenging functions, the main one being to remove the heat produced by particle bombardment, radiation and volumetric nuclear heating, while ensuring gas streaming channels towards the pumping ports for alpha particles, impurities and unburnt D-T fuel.

In addition, the DIV provides shielding for the VV and magnets against nuclear loads, while also offering a plasma-facing surface physically compatible with the plasma (low sputtering and tritium retention, high melting point) [13].

To guarantee stable fusion operation, the amount of ashes in the burning plasma, namely the alpha particles after they have transferred their kinetic energy to the plasma, must be controlled by limiting their concentration. The control is achieved through the diverted magnetic configuration, which directs the exhaust particles along open field lines towards the DIV targets.

In particular, the mixture of ashes, impurities, and unburnt D-T fuel which drifts outwards the core plasma crosses the separatrix, also referred to as Last Closed Flux Surface (LCFS), and enters the Scrape-Off Layer (SOL), a region of the plasma characterised by open magnetic field lines. The particles are guided along the SOL field lines and deflected towards the DIV Vertical Targets (VTs), where they are neutralised and pumped out from the plasma chamber together with the produced impurities (figure 1.6).

Once the particles reach the surface of VTs, they transfer their kinetic energy to the component, thereby generating high heat fluxes in the DIV. The thermal power deposited on the component has to be continuously and actively removed through cooling to ensure steady-state or long-pulse operations.

Furthermore, since the power decay length in SOL is small, the thermal power

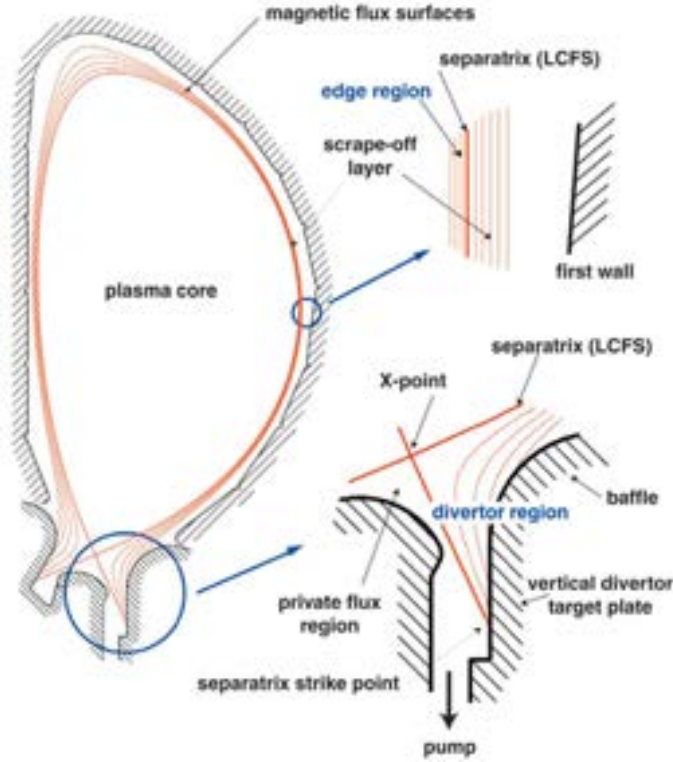


**Figure 1.6:** Simplified depiction of the typical magnetic field structure in the edge region of a standard tokamak operating with a diverted magnetic topology (poloidal cross-section) [13].

density is concentrated on a restricted band of the targets and, in particular, leading to a local peak heat flux to the so-called Strike Point (SP).

The key plasma parameters of DEMO associated with the power exhaust and relevant for the present dissertation are reported in table 1.1, with a brief description of quantities and retrieved from the reference design [14].

**Table 1.1:** Relevant parameter of DEMO device

| Quantity          | Values | Units | Description                   |
|-------------------|--------|-------|-------------------------------|
| Pulse             | 7200   | s     | Fusion plasma duration        |
| $P_{\text{fus}}$  | 2000   | MW    | Fusion power                  |
| $R_{\text{geom}}$ | 8.94   | m     | Major radius of machine       |
| A                 | 3.1    | -     | Aspect ratio                  |
| $B_{\text{tor}}$  | 4.89   | T     | Toroidal magnetic field       |
| $q_{95}$          | 3      | -     | Safety factor                 |
| $\kappa_{95}$     | 1.65   | -     | Elongation                    |
| $\delta_{95}$     | 0.33   | -     | Triangularity                 |
| $P_{\text{sol}}$  | 151    | MW    | Power to the Scrape-Off Layer |
| $I_p$             | 19.7   | MA    | Plasma current                |

To mitigate the power density concentration on VTs, two solutions are generally employed:

- The VTs are slightly inclined with respect to the grazing magnetic field lines in order to enhance the projected surface area [15];

- Inert gases are injected to promote power dissipation via radiation from the SOL, leading to a detached plasma condition [16].

The adoption of these technical solutions allows for a substantial reduction in the heat fluxes so that VTs work within the operational limits of current technologies. Nevertheless, both normal and off-normal plasma transients can still impose extreme thermal loads, reaching several tens of MW/m<sup>2</sup>. Accordingly, a dedicated section will be presented later (section 1.3.4), defining and discussing the mitigation strategy in the case of loss of detachment, which is foreseen for DEMO.

### 1.3.1 The Divertor normal operation power loads

According to the DEMO baseline [14] which considers the production of 2 GW of fusion power, in the case of the WCLL BB configuration, the power from the alpha particles and auxiliary heating is distributed as follow: 302 MW are connected to core radiation, while 151 MW cross the SOL. Assuming a power related to charged particles of 75 MW [17], the remaining portion of the power crossing the SOL is attributed to radiative losses, resulting in a SOL radiative power of 76 MW (figure 1.7a). This assumption is not derived from a detailed power deposition profile, which is unavailable, but rather reflects a pragmatic engineering choice aimed at optimising the DIV performance.

Indeed, it has been observed in [17] that a power related to charged particles of 75 MW causes in the DIV surface a tolerable heat flux with a maximum peak below the 5 MW/m<sup>2</sup> which is within safe DIV operational limits. Consequently, the remaining power is assumed to be radiated in the SOL, which allows for a reduction in the required impurity injection to achieve plasma detachment. Minimising the amount of injected inert species is beneficial for maintaining core plasma stability, as excessive impurity levels can degrade confinement and increase radiative losses in undesired regions.

This power split reflects a balance between maintaining DIV integrity, facilitating detachment, and preserving plasma performance, in the absence of a detailed predictive model for power distribution in the SOL.

In analogy to [17], the total heat flux deposition profile estimated for a detached DIV in steady-state conditions, as well as the heat flux breakdown, can be calculated. The figure 1.8a shows the heat flux in the DIV where the peak is less than 5 MW/m<sup>2</sup>.

Therefore, integrating the heat flux profiles over the pertaining surfaces and considering the nuclear heating, the overall power deposited on the DIV PFC is defined as in table 1.2a.

The described power distribution can be revised in order to consider more severe load conditions to DIV, increasing the SOL total power to 20 %, conservatively channelling this increased power to the particles impinging on the VTs as done in [17]. Therefore, it is possible to obtain a distinct breakdown (figure 1.7b) and



|                            | OVT | IVT | Total |
|----------------------------|-----|-----|-------|
| <b>Core Radiation [MW]</b> | 3   | 2   | 5     |
| <b>SOL Radiation [MW]</b>  | 11  | 12  | 23    |
| <b>Particles [MW]</b>      | 40  | 23  | 63    |
| <b>Neutrons [MW]</b>       | 14  | 12  | 26    |
| <b>Total [MW]</b>          | 68  | 48  | 117   |
| (a)                        |     |     |       |
|                            | OVT | IVT | Total |
| <b>Core Radiation [MW]</b> | 2   | 2   | 4     |
| <b>SOL Radiation [MW]</b>  | 16  | 16  | 32    |
| <b>Particles [MW]</b>      | 41  | 23  | 64    |
| <b>Neutrons [MW]</b>       | 14  | 12  | 26    |
| <b>Total [MW]</b>          | 73  | 53  | 126   |
| (b)                        |     |     |       |

**Table 1.2:** Integral power breakdown for the DIV PFCs: baseline (a) and severe condition (b).

integral power breakdown (table 1.2b). Similarly, the heat flux acting on the VTs changes, observing a peak in both components much closer to  $5 \text{ MW/m}^2$  as shown in figure 1.8b.

In this dissertation, these two scenarios will be adopted. The first case, with a SOLs total power of 151 MW, will be used to analyse the thermal-hydraulic behaviour of the DIV PFC in-vessel cooling circuit, while the more severe condition related to a total power of 181 MW, will be adopted when analysing accidental transients with coolant release from the DIV PFC PHTS.

### 1.3.2 General description of the Divertor design

According to the DEMO baseline, the DIV is articulated in 48 toroidal cassettes, three for each sector, which are nearly identical and spaced 20 mm apart, thereby facilitating remote handling operations. As shown in figure 1.9, the toroidal angular range of a sector is  $22.5^\circ$  so that a single module occupies the toroidal angular range of  $7.5^\circ$  and is mechanically bound to the bottom of the VV.

Considering the DEMO baseline DIV design [18], each cassette (figure 1.10) is composed of:

- Cassette Body (CB): holds all sub-components of the cassette module and protects the VV from radiation and neutron flux;
- Shielding Liner (SL): posed on CB aims to shield the steel structure, as CB, from neutron flux, radiation and gas particles;
- Reflector Plates (RPs): willing in the CB safeguard the feeding pipes of the targets and the manifolds;

- PFCs: distinguished in Inner Vertical Target (IVT) and Outer Vertical Target (OVT) intersecting the SOL and housing the Plasma Facing Units (PFUs).

The VTs are the most significant and technologically critical components, since they must sustain extreme thermal loads where the local peak heat flux can reach values in the order of  $20 \text{ MW/m}^2$  during normal and off-normal operation conditions, such as a slow transient connected to the plasma reattachment.

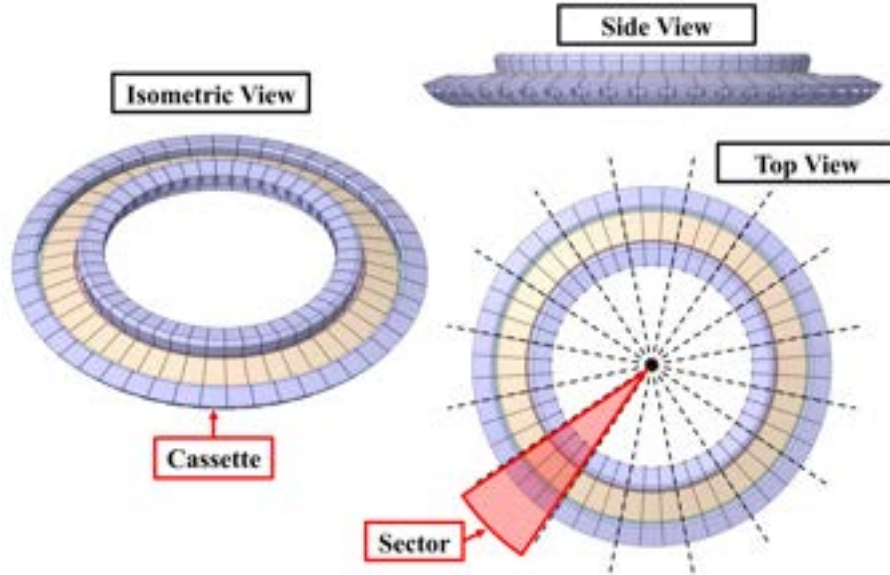


**Figure 1.9:** Comprehensive CAD model of the full DEMO DIV, presented from three different viewing perspectives.

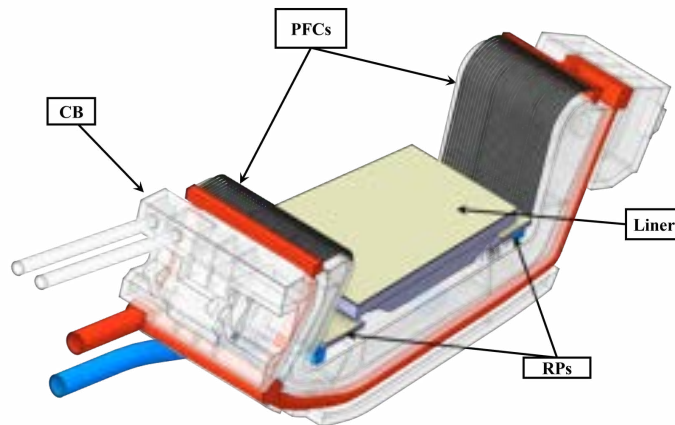


**Figure 1.10:** DIV CAD model 2019.

The IVT and OVT consist of a certain number of PFUs assemblies, 31 and 43 channels, respectively. Each assembly comprises ITER-like PFUs [19], characterised by a cooling tube of Copper-Chromium-Zirconium (CuCrZr) alloy joined to an array of Tungsten (W) armour Monoblocks (MBs) with a thin (1 mm) Copper

(Cu) interlayer to accommodate their differential thermal expansions. The CuCrZr alloy tube contains a thin Cu Swirl Tape (ST) turbulence promoter to enhance heat transfer performance. The W-MB is a parallelepiped structure with a width of 23 mm, height of 28 mm and depth of 12 mm. Furthermore, a front side armour thickness of 8 mm has been selected in order to extend the erosion lifetime. A sketch of a single PFUs assembly as well as the current DEMO MB are visible in the figure 1.11.

Due to different heat loads and to guarantee structural material integrity, the CB together with the SL and RPs are cooled with a system separated from the PFCs. Indeed, contrary to PFCs, CB and other sub-components experience a significantly lower heat flux, primarily influenced by power deposition from nuclear heating.

According to the DEMO DIV 2019 baseline [18], the CB cooling system must ensure sub-cooled water at a pressure of 3.5 bar, with inlet and outlet temperatures of 180 °C and 210 °C, respectively. Meanwhile, to withstand intense heat fluxes and maintain a safe margin against the occurrence of Critical Heat Flux (CHF), the PFCs require sub-cooled water at 50 bar, with inlet and outlet temperatures of 130 °C and 136 °C.

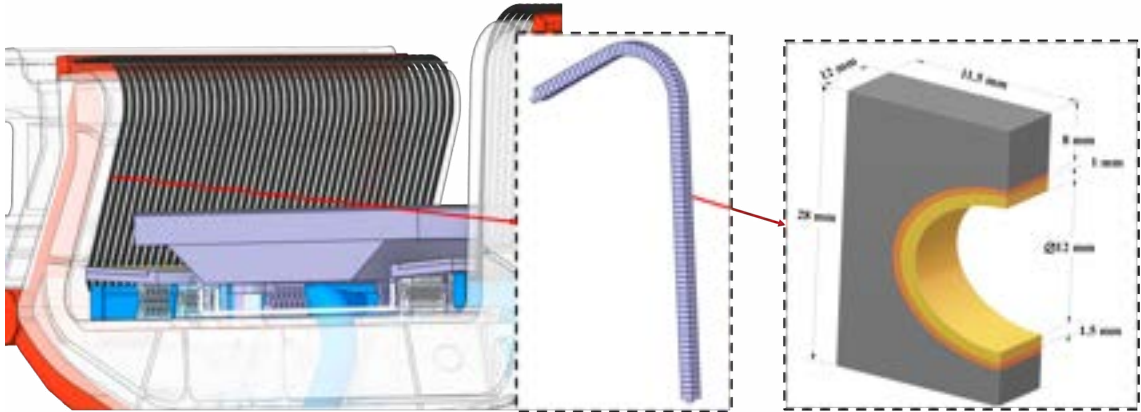


**Figure 1.11:** Detailed view of an OVT PFUs assembly and half cross-section of the current DEMO monoblock, highlighting key geometrical features.

### 1.3.3 The in-vessel Plasma Facing Components cooling circuit

Concerning the in-vessel cooling circuit of the PFCs, the IVT and OVT are arranged in a parallel configuration, as illustrated in figure 1.12, which reports the coolant temperatures, pressures, and mass flow rates, in accordance with [20, 21].

As already observed, the cooling circuit is designed to withstand the exceptional heat fluxes with a peaked heat flux value close to 20 MW/m<sup>2</sup> during normal and off-normal operation transient conditions. Therefore, high mass flow rates of low-temperature water are required, resulting in average velocities within the

PFUs cooling channels in the range of 12 to 15 m/s [20]. This approach ensures sufficient margin against the occurrence of CHF to avoid dry-out phenomena that could compromise the structural integrity of the component.

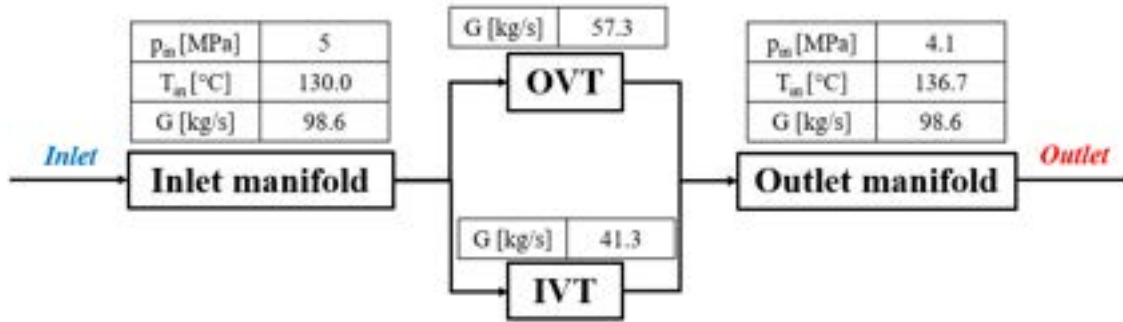


**Figure 1.12:** Schematic flowchart of the PFCs in-vessel cooling circuit [21].

Concerning the geometrical configuration reported in figure 1.13, the VTs are linked to the main inlet and outlet manifolds through three-way junctions, which distribute and collect the cooling water directed to their respective PFUs assemblies.

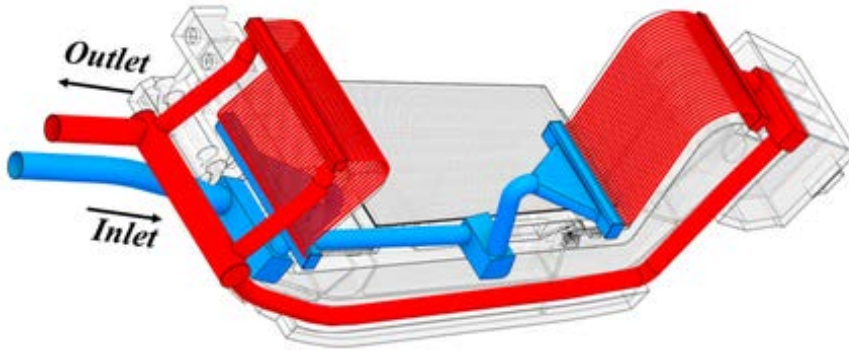


**Figure 1.13:** DEMO DIV PFCs in-vessel cooling circuit.

### 1.3.4 Challenges and Requirements of the Divertor

The DEMO device is assumed to operate with a detached DIV in order to avoid high thermal loads in normal operation conditions, which cannot be dealt with by the current state-of-the-art high heat flux technology.

This condition will be achieved in the reactor with the injection of radiative impurities through which the power exhaust is released onto the first wall in the form of electromagnetic radiation.

Although the detachment condition is essential for the normal and safe operation of the DIV, it can be lost. The DIV reattachment on the target plate can happen:

- when the impurities injection system fails;
- after a significant fluctuation on  $P_{sol}$ ;
- during a density fluctuation in the DIV region;

- when one or more pellet injectors jam [22].

In the case of reattachment, the thermal load experienced by the DIV can be estimated by the Eich equation [23] as reported below, in which  $\bar{s}$  is the difference between the generic axial position on the VT and the SP.

$$q''(\bar{s}) = \frac{q_0''}{2} \cdot \exp\left(\left(\frac{S}{2\lambda_q}\right)^2 - \frac{\bar{s}}{\lambda_q f_x}\right) \cdot \operatorname{erfc}\left(\frac{S}{2\lambda_q} - \frac{\bar{s}}{S f_x}\right) + q_{BG}'' \quad (1.5)$$

$$\bar{s} = s - s_0 \quad (1.6)$$

The  $q_{BG}$  parameter is the background heat flux, accounting for the portion of heat flux not associated with particle contributions. Since its magnitude is significantly lower than that of the particle-induced heat flux, it has been neglected for the purposes of this dissertation. The  $f_x$  is the effective flux expansion, which is calculated as reported in equation (1.7), considering [24] and [22] where  $f_{mag} = 0.29$  and  $\eta = 30^\circ$  that represents the angle between the striking magnetic field line and the VT in the poloidal plane.

$$f_x = \frac{1}{f_{mag} \cdot \sin(\eta)} \quad (1.7)$$

Concerning  $\lambda_q$  and  $S$ , they are respectively the decay length at the outer midplane and the power spreading factor. According to [22], they are assumed to be equal. The last undefined parameter in equation (1.5) is the term  $q_0''$ , which is the total heat flux on the VT, calculated according to equation (1.8) where in the case of OVT the  $P_{VT} = (2/3) \cdot P_{sol}$  and  $R_{VT}$  is the radius of the target.

$$q_0'' = \frac{2 \cdot P_{VT}}{2 \cdot \pi \cdot R_{VT} \cdot \int_{-\infty}^{+\infty} q''(\bar{s}) d\bar{s}} \quad (1.8)$$

Taking into account [22], the equation (1.8) can be revised, and the term  $q_0''$  can be defined as follows in equation (1.9).

$$q_0'' = \frac{P_{VT} \cdot f_{mag} \cdot \sin(\eta)}{2 \cdot \pi \cdot R_{VT} \cdot \lambda_q} \quad (1.9)$$

Furthermore, the decay length  $\lambda_q$  can be calculated by applying the Eich scaling law [23]. The value, expressed in mm, can be obtained from equation (1.10), where the  $q_{cyl}$ , which is the cylindrical safety factor, can be approximated as  $q_{95}$ .

$$\lambda_q = 0.86 \cdot B_{tor}^{-0.8} \cdot q_{cyl}^{1.11} \cdot P_{sol}^{0.11} \cdot R_{geo}^{0.13} \quad (1.10)$$

Therefore, taking into account the Eich equation and substituting the terms considering the reference DEMO baseline [14] and [25], the OVT, which is the target subject to the highest load under attachment conditions, is characterized at the SP

by a heat flux on the order of  $83 \text{ MW/m}^2$ , in line with the results discussed in [26]. The expected heat flux profile in the case of loss of detachment at OVT, calculated according to (1.9) and using the input values listed in table 1.1, is reported in figure 1.14.

As it can be argued from the picture, the entire power is deposited onto a region with a poloidal extension of  $\approx 10 \text{ cm}$ , in line with [13].

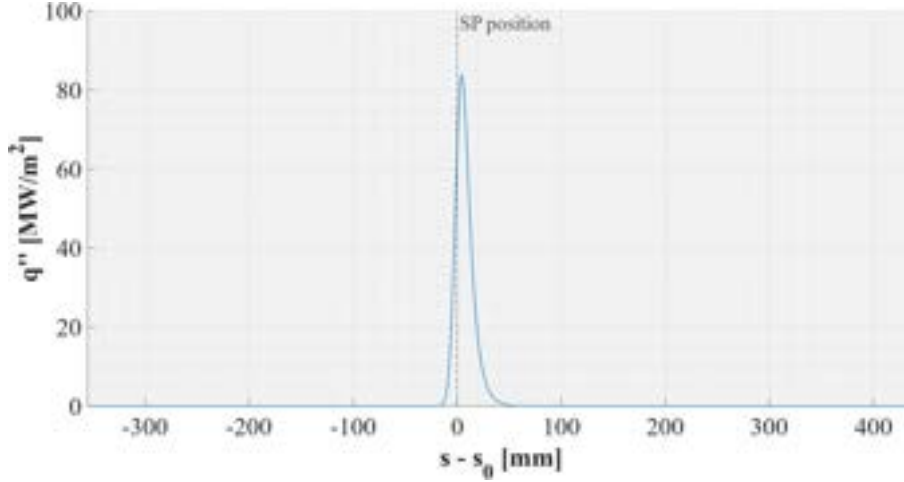


**Figure 1.14:** Outer target power parallel heat flux for DEMO baseline [14, 25].

In light of the discussion presented so far, it is evident that a mitigation strategy must be foreseen in case of loss of detachment, to cope with significant heat fluxes of this magnitude, thereby ensuring the safe shutdown of the reactor.

One of the potential solutions is the plasma SP sweeping, which is a countermeasure already used in existing devices [27, 28].

This technique is applied to the strike lines by means of dedicated in-vessel coils, which generate a time-varying magnetic perturbation that periodically shifts the SP position along the DIV target. The resulting movement (see figure 1.15), characterised by a selected amplitude ( $A$ ) and frequency ( $f$ ), allows the reduction of concentrated thermal loads through the spreading of the total energy over a wider target area.

The advantage of the aforementioned mitigation technique for the DEMO device has already been assessed in [29] and [30], although the studies refer to a different reactor baseline. Therefore, as reported in [29], the choice of the proper sweeping parameters has to be made in order that:

- the CHF conditions are not achieved;
- the W maximum temperature is below the melting limit of  $3422 \text{ }^\circ\text{C}$  and a recrystallisation temperature of around  $1200 \text{ }^\circ\text{C}$ ;
- the CuCrZr maximum temperature does not achieve the limit of  $350 \text{ }^\circ\text{C}$ , above which the material starts to soften/age.

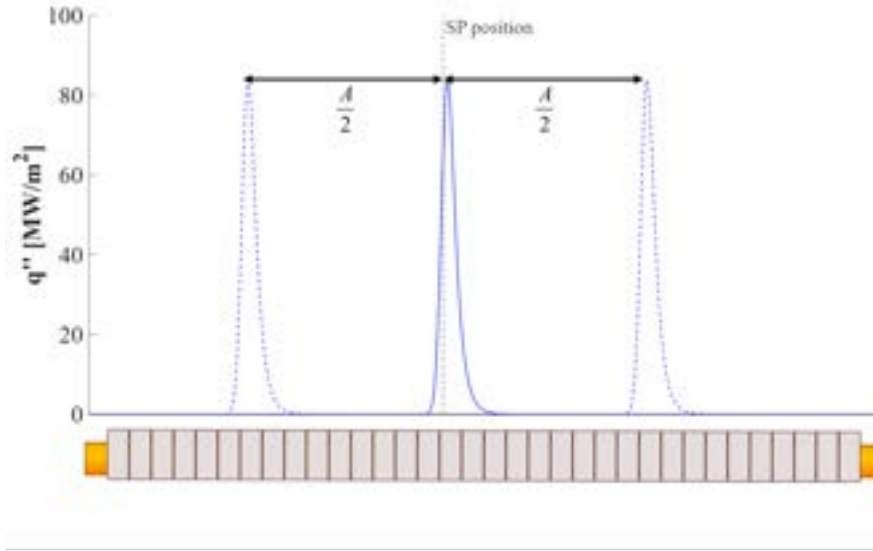


**Figure 1.15:** Schematic drawing of sweeping the heat flux in case of loss of detachment.

## 1.4 The Primary Heat Transport System

The main function of the PHTS is to extract thermal power from the in-vessel components and transfer it to the PCS through an intermediate element that will be a steam generator in the case of the BB PHTS, and Heat eXchangers (HXs) for the DIV PHTSs and the VV PHTS.

The presence of multiple coolant systems is due to the varying requirements of the components they serve, as reported in table 1.3 [31].

**Table 1.3:** Input thermal power and temperatures of PHTSs.

|                                         | BB        | VV    | PFC     | CB      |
|-----------------------------------------|-----------|-------|---------|---------|
| <b>Power [MW]</b>                       | 1723-1884 | 11    | 96-142  | 171-185 |
| <b><math>T_{in}/T_{out}</math> [°C]</b> | 295-328   | 40-60 | 130-136 | 180-210 |

Specifically, the DIV PHTSs behave like pre-heaters to the PCS feed-water and their working pressure and temperature ranges are imposed by the operating conditions of the in-vessel client cooling circuit, as reported in [32].

Although the thermal-hydraulic behaviours are different, both DIV PHTSs layouts are the same.

Therefore, the layout of the systems will be described below, providing a brief overview of the changes they have undergone, followed by a focus on the description of the ex-vessel circuit and equipment serving the PFCs. In addition, the control systems of the PHTS, which are essential for normal operation, will be addressed. Subsequently, the transient scenarios in which these systems are assumed to be unavailable will be defined. Finally, the associated safety system designed to

mitigate the consequences of such transients, namely the Vacuum Vessel Pressure Suppression System (VVPSS), will be introduced.

### 1.4.1 The evolution of Divertor Primary Heat Transport Systems

Over time, within the EUROfusion activities, the DIV PHTSs design underwent various modifications. In particular, until 2020, during the DEMO pre-conceptual phase, the PHTSs were subdivided into two independent cooling loops, each one feeding eight Divertor sectors [33, 34]. The loop (figure 1.16) consisted of the in-vessel circuits, the HX, the Pressurizer (PRZ), the main coolant pump and the connecting pipes. Hot (cold) manifolds of each circuit are arranged to form a hot (cold) half-ring, which collects (distributes) the coolant among the feeding pipes.

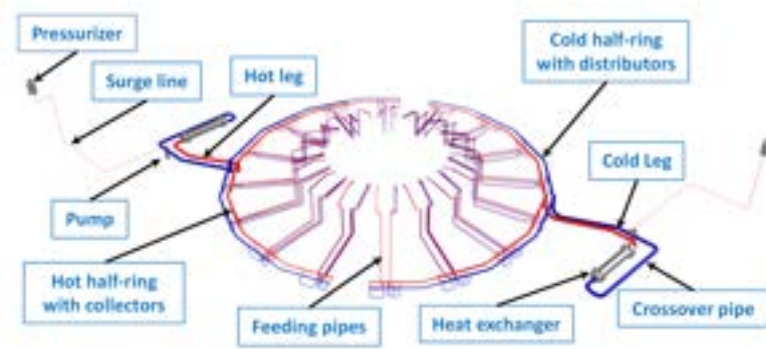


**Figure 1.16:** Overview of previous DIV PHTS.

Since critical issues have been observed during transient scenarios that could compromise the safety and reliability of the circuit, the layout has been revised [35].

In particular, a single cooling loop configuration has been proposed, providing bi-directional flow in both PHTSs.

The cooling circuit will consist of two levels figure 1.17.

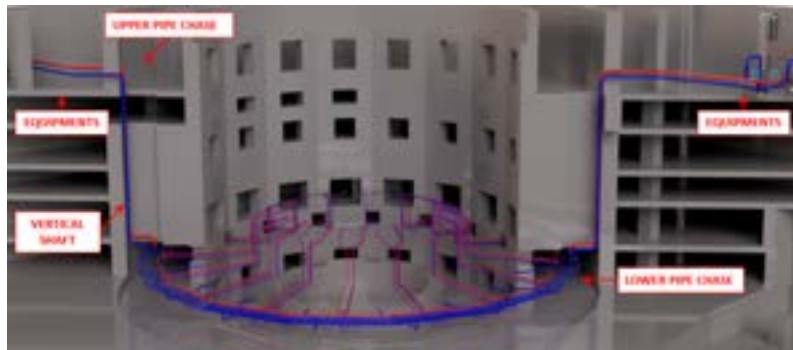


**Figure 1.17:** Concept schemes for loops arrangement.

The in-vessel components will be fed through the lower ports, and the main water distribution will be carried out into the lower pipe chase, where ring headers

are placed. Instead, the main equipment, as HXs will be located on the upper level of the tokamak building, and they will be connected to the headers through the hot and cold legs located in Vertical Shaft (VS).

The current cooling loop figure 1.18 feeds sixteen sectors and consists of the in-vessel cooling circuits, two HXs, a PRZ, two main coolant pumps and the connecting pipes. Hot (cold) manifolds are arranged to form a hot (cold) ring, which collects (distributes) the coolant among the feeding pipes.

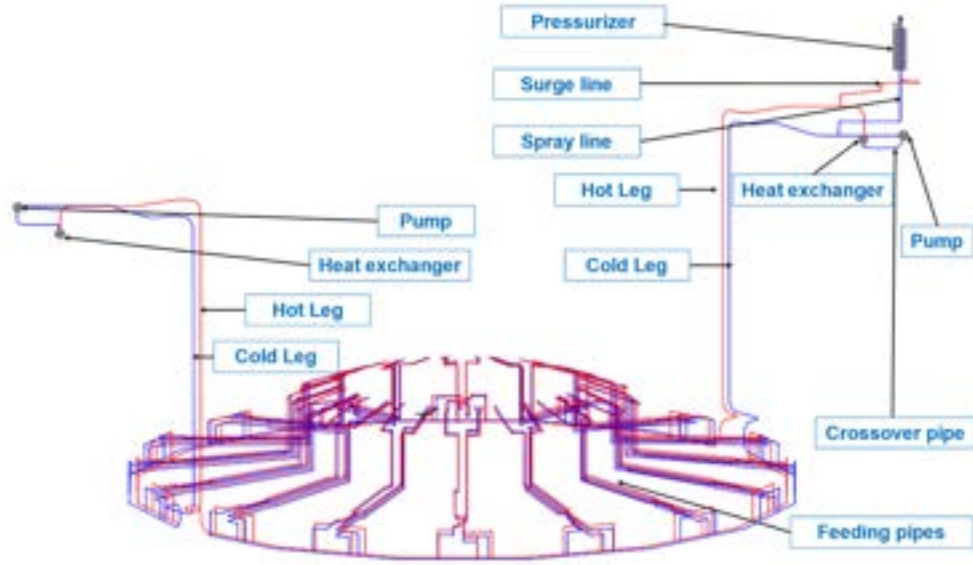


**Figure 1.18:** Overview of current DIV PHTS.

The arrangement allows for a well-defined area of the building to be reserved for all main components (excluding piping) of the different coolant systems. Moreover, this layout is expected to enhance natural circulation.

### 1.4.2 Technical overview of the Plasma Facing Components Primary Heat Transport System

The following section addresses the geometric aspects and key design parameters of the PFCs PHTS, with the aim of characterising the essential features for the development of the thermal-hydraulic models proposed in this dissertation. Further details, beyond the scope of this introductory section, can be found in [36].

Starting from the piping, the PHTS consists of six DN600 pipe sections - two hot legs, two cross-over legs (also referred to as loop seals), and two cold legs - along with additional smaller-diameter ducts. To reduce the coolant inventory while maintaining acceptable pumping power requirements, the hot and cold rings are designed with diameters ranging from DN600 to DN300. Additionally, the feeding pipes which supply the 48 in vessel PFC cooling circuit are designed with a DN125, while the surge and spray line feature respectively a DN100 and DN50. The pipe

sections are of stainless steel (AISI 316LN) and are coated with the microporous insulation Microtherm® SLATTED [37].

Furthermore, the cooling circuit includes two Shell-and-Tubes TEMA type BEU HXs, the schematic view of which is shown in figure 1.19. Concerning the material, the HXs are constructed primarily of carbon steel, while the heat transfer tubes are made of Inconel. The main design parameter of both HXs are reported below in table 1.4.

**Table 1.4:** *Thermal-hydraulic parameters of the HX.*

|                                           | Primary coolant<br>(tube side) | Secondary coolant<br>(shell side) |
|-------------------------------------------|--------------------------------|-----------------------------------|
| Inlet pressure [MPa]                      | 3.9                            | 1.0                               |
| Inlet temperature [°C]                    | 136.00                         | 57.4                              |
| Outlet temperature [°C]                   | 129.24                         | 105.9                             |
| Mass flow rate [kg/s]                     | 2368                           | 338.63                            |
| Number of tubes                           |                                | 941                               |
| Number of baffles                         |                                | 3                                 |
| Fouling factor [ $\text{m}^2\text{K/W}$ ] | 0                              | 8.8E-5                            |
| Heat transfer surface [ $\text{m}^2$ ]    |                                | 625.40                            |
| Tube external diameter [mm]               |                                | 25.4                              |
| Tube pitch [mm]                           |                                | 38                                |
| Tube thickness [mm]                       |                                | 1.245                             |
| Tube active length [m]                    |                                | 8.728                             |
| Primary volume [ $\text{m}^3$ ]           |                                | 3.386                             |
| Secondary volume [ $\text{m}^3$ ]         |                                | 10.55                             |

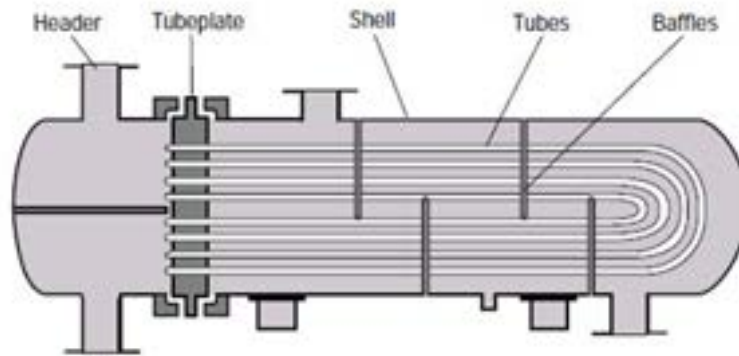


**Figure 1.19:** *Schematic view of a TEMA type BEU HX [36].*

The loop is equipped with a PRZ in carbon steel, and the relevant parameters are reported in table 1.5. At last, the main aspects of both pumps are defined in table 1.6.

**Table 1.5:** *Pressurizer sizing summary.*

| Parameter                                               | Value  |
|---------------------------------------------------------|--------|
| PRZ Pressure [MPa]                                      | 3.69   |
| PRZ temperature (saturation condition) [°C]             | 245.6  |
| Cylinder internal volume [m <sup>3</sup> ]              | 20.925 |
| Single head internal volume [m <sup>3</sup> ]           | 0.898  |
| Total internal volume [m <sup>3</sup> ]                 | 22.721 |
| Liquid volume during normal operation [m <sup>3</sup> ] | 13.594 |
| Vapour volume during normal operation [m <sup>3</sup> ] | 9.127  |
| Internal diameter [m]                                   | 1.900  |
| Cylinder height [m]                                     | 7.380  |
| Total internal height [m]                               | 8.330  |
| Cylinder height to diameter ratio [-]                   | 3.880  |
| PRZ cross-section area [m <sup>2</sup> ]                | 2.835  |
| Heaters power [kW]                                      | 314    |
| Heaters length [m]                                      | 1.200  |
| Heaters number [-]                                      | 12     |

**Table 1.6:** *Main coolant pumps design parameters.*

| Parameter                                      | Value |
|------------------------------------------------|-------|
| Number of pumps [-]                            | 2     |
| Inlet pressure [MPa]                           | 3.53  |
| Inlet temperature [°C]                         | 130   |
| Rated volumetric flow rate [m <sup>3</sup> /s] | 2.528 |
| Rated head [m]                                 | 204   |
| Isentropic efficiency [-]                      | 0.80  |
| Electrical/mechanical efficiency [-]           | 0.95  |
| Thermal power per pump [MW]                    | 5.933 |
| Electrical power per pump [MW]                 | 6.245 |

### 1.4.3 Control systems in the Plasma Facing Components Primary Heat Transport System

Generally, the thermal-hydraulic parameters in an Nuclear Fission Power Plant (NPP) must be strictly monitored during plant operation. As a result, the main equipment works always within its design limits.

In DEMO, the excursion of the main thermal-hydraulic parameters of PHTS must also be limited to mitigate the potential negative effects of the pulsed operations, which may induce undue thermal and mechanical cycling.

In NPP, electronic and mechanical systems are put into place which give the operating staff either manual or automatic control over system behaviour. In manual mode, the operator receives data from sensors located throughout the system and consciously decides which component actions need to take place to achieve the desired effect. In automatic mode, electronic circuits exist which can take some of the intervention away from the operator to automatically control certain functions.

Parameters like temperature, water level, pressure, flow rate, and power can be detected, and using predetermined set points, the electronic logic decides how to adjust the various components so that control is maintained [38].

Therefore, to regulate the DIV PFC PHTS parameters during the pulse-dwell transition, three main control systems are essential, namely:

- the PRZ pressure control system;
- the PRZ level control system;
- the feed-water flow control system.

If the pressure in the PRZ decreases from the desired value reported in table 1.5, the master controller increases the output of the proportional heaters. If the pressure continues to decrease, all banks of backup heaters are turned on. As the heaters add energy to the water in the PRZ, more water flashes to steam, thereby increasing the pressure to the desired setpoint.

Similarly, as the pressure in the PRZ increases above its normal setpoint, the master controller decreases the output of the proportional heaters. If the pressure continues to increase, the master controller output modulates the spray valves open.

In the case of a large pressure transient, safety valves are provided on the PRZ as a final means of protecting the integrity of the cooling circuit.

The logic described so far in the case of PRZ pressure excursion scenarios is illustrated in the figure 1.20a and figure 1.20b, where the actions of the heaters and the valve fraction area are a function of the pressure error. The PRZ level control system functions to maintain the proper water inventory in the primary coolant system. This inventory is maintained by controlling the balance between water leaving and entering the system. An unchanging PRZ level typical of steady-state conditions indicates that a balance exists between the charging flow and the letdown flow into the Chemical and Volume Control System (CVCS). In the case of transient scenarios, the level changes due to the primary coolant expansion and contraction associated with the variation of the coolant average temperature.

To determine the PRZ level, the difference in pressure between an external column of water of a known height (reference leg) and a variable column of unknown height inside the PRZ (variable leg) must be measured and then converted into a level signal. If a difference exists between the measured level and the reference one, the controller varies the flow at CVCS.

Additionally, the level signal is sent to a trip that provides a low-level interlock to protect the PRZ heaters from being damaged if operated in a steam environment. Both minimum and maximum level limitations are placed on the level program as shown in figure 1.21.

At last, the feed-water demand is influenced by both the average coolant temperature and the extracted power during the duty cycle. Specifically, when the average temperature error is high, the feed-water flow must increase; conversely,

it should decrease when the error is low.

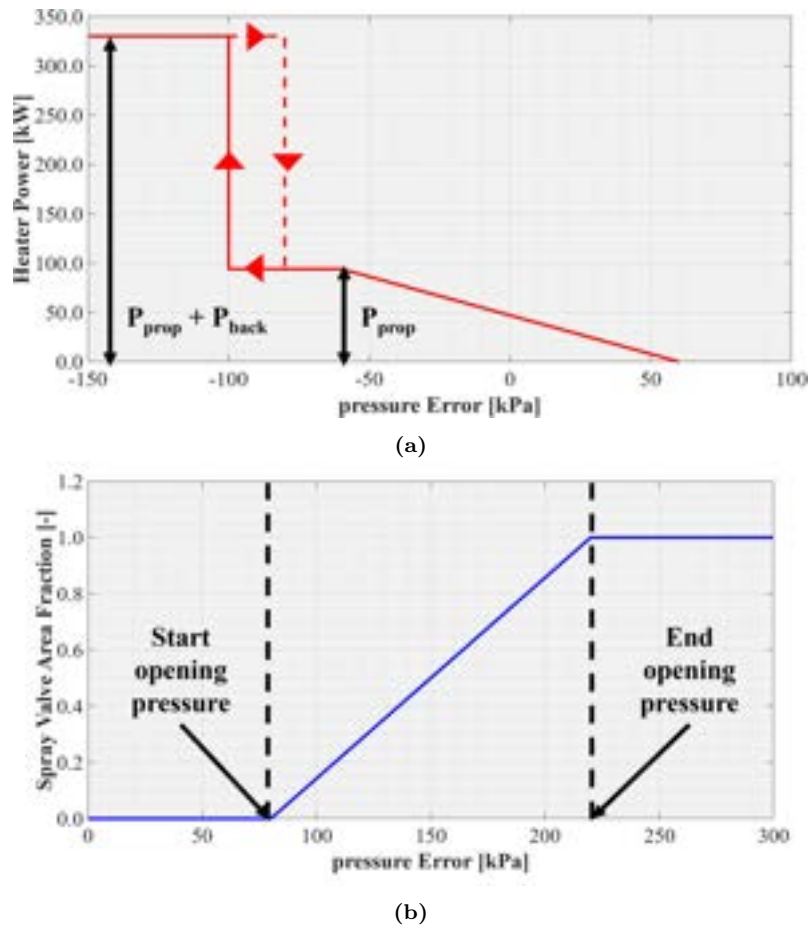


**Figure 1.20:** Control system response to pressure error: heater power modulation (a) and emergency spray valve opening fraction (b).

Referring to the extracted power, since the heat generation (reactor power) must be equal to heat removal (feed-water flow), a normalised power based on the pulse value must be calculated. The corresponding feed-water flow is then selected from a predefined trend linked to this normalised power. This value is combined with the correction derived from the average temperature error to determine the final feed-water flow.

Additionally, due to the peculiar PHTS layout, the effective feed-water flow is also influenced by the difference between the one-half loop and the second one cold leg inlet temperatures. This further adjustment ensures a balanced exchange of thermal energy between the HXs.

The described control systems are supposed to be unavailable during an accidental scenario. In such cases, the transient identification systems are tailored to the specific scenario and may account for interactions with other systems within the plant, such as the VV in the case of in-vessel LOCA.

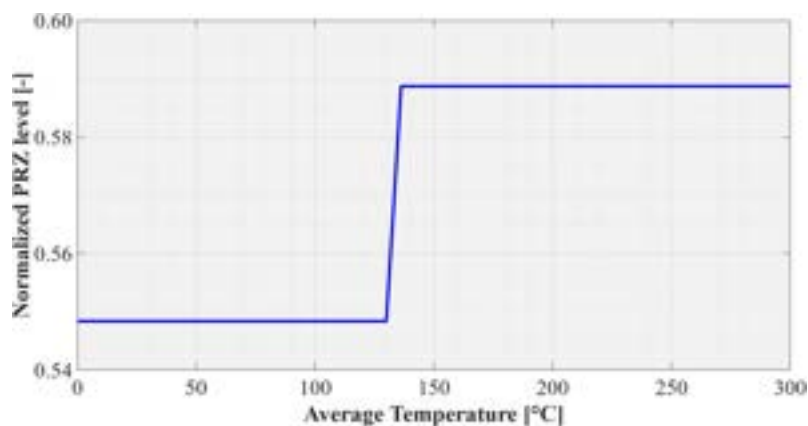


Figure 1.21: Pressurizer level as a function of coolant average temperature.

## 1.5 Transient accident scenarios: critical cooling failures in Divertor circuit

As is the case for fission-based facilities, the design of a NFPPs also involves the identification and delineation of the Postulated Accidents (PAs), which are unanticipated events or transients that are not expected to occur over the life of the plant, but which could cause the release of radioactive materials from the plant. The analysis of certain PAs allows for assessing and confirming the adequacy of the plant safety systems, including the Engineered Safety Features (ESFs).

Among PAs, a subset known as Design Basis Accidents (DBAs) is explicitly accounted for in the design and licensing process of the power plant. DBAs are characterised by well-defined acceptance criteria in terms of parameters to be controlled or limits not to be exceeded, and are used to ensure that safety systems can adequately mitigate their consequences.

In this context, the LOCA is one of the leading DBAs: it involves a breach in the reactor coolant system. Such an event can occur in various sections of the circuit and may vary significantly in size (from small leaks to large-scale ruptures), each posing different challenges for the safety systems of the power plant.

According to the selected reference accident scenarios for DEMO [39] and [40], the DIV PFC PHTS can be experienced both in-vessel and ex-vessel LOCA.

The in-vessel LOCA is associated with the cooling channels break in the PFCs circuit, which is the initiating event. As a consequence of the event, the following chain of accidents can occur:

- loss of coolant into VV;
- VV pressurisation;
- pressure relief to VVPSS (see section 1.6 for more details);
- VV radioactive products and tritium released into VVPSS room and the VV surrounding area through containment leaks;

- possible loss of VV penetration leak-tightness due to the high overpressure inside the plasma chamber.

The release of coolant inside the VV, defined as Ingress of Coolant Event (ICE), as reported above, causes a more or less severe pressure increase depending on the inventory of coolant discharged. In any case, the pressure rise should be kept below the design value 0.2 MPa as in ITER [41].

On the other hand, the ex-vessel LOCA, which more closely resembles those typical of NPPs, is connected to a rupture in any section of the circuit outside the vessel. This implies a case-by-case differentiation. In particular, the release associated with LOCA can occur in the tokamak building or in the secondary water loop because of the rupture of tubes in the HX. The first case, which is the focus of the present dissertation, is characterised by the following chain of accidents:

- coolant discharge in the building;
- possible tritium release and activated products from the cooling loop released into the room and potentially to the environment;
- rise of temperatures inside the DIV in case of failure to actively shutdown the plasma;
- thermo-mechanical stress on the DIV structures due to the overheating and plasma disruption;
- possible collapse of the DIV;
- ingress of coolant inside the VV due to leakage from DIV in-vessel cooling circuit;
- occurrence of the same chain of the in-vessel LOCA;
- amount of radioactive products and tritium released into the building;
- worker over-exposure due to the whole accident sequence;
- worker over-exposure due to recovery actions;

## 1.6 The Vacuum Vessel and the related Safety Systems

The VV is a toroidal structure with D-shaped cross section located inside the Cryostat and surrounded by the magnetic confinement system. It encloses the plasma and houses the in-vessel component. The VV consists of 16 sectors, each one includes an upper, an equatorial and a lower port (figure 1.22).

As previously noted (section 1.2), it must fulfil various requirements, and its main function is to ensure a high-vacuum environment for plasma operation. Additionally, it represents the first and main nuclear confinement barrier acting as neutron and radiation shielding for the superconducting coils and all maintenance areas [6].

The VV is planned to be constructed as a double shell structure, and the

neutron shielding is obtained by optimising the amount of steel between the inner and outer plates of its double-wall. The component must safely withstand any anticipated loading conditions, including earthquake, gravity, coolant pressure, and any combinations thereof [42].

The VV must also remove, through its PHTS, the heat load resulting from nuclear heating during plasma operation, as well as in the unlikely event of a major technical malfunction.

The VV is equipped with a passive safety system designed to manage the rupture of in-vessel components such as the DIV and BB, and the resulting release of coolant into the plasma chamber, as illustrated in figure 1.23. Specifically, the VVPSS ensures primary confinement of the radioactive inventory under accidental conditions associated with the coolant release in the plasma chamber. It has been designed to maintain the pressure inside the VV below 0.2 MPa by condensing the generated steam.

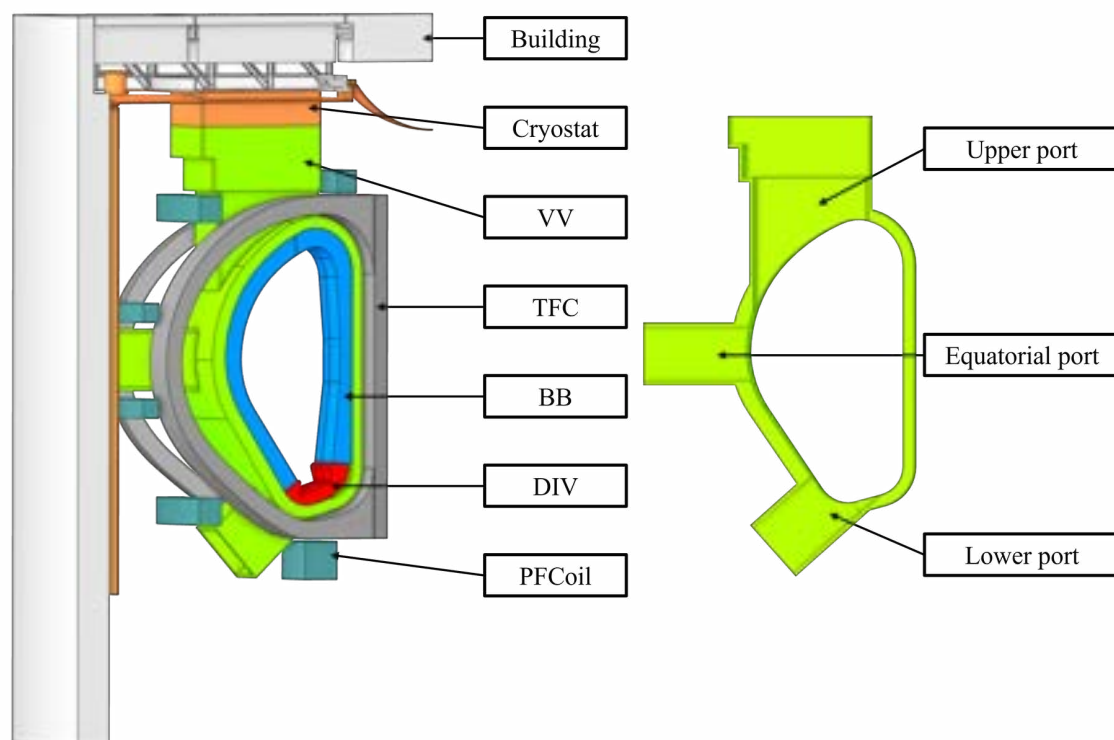


**Figure 1.22:** Overview of a single VV sector (left) and focus on the ports (right).

The safety system consists of two subsystems designed to manage accidental transients where the amount of released vapour is distinct. In case of limited releases, associated, for example, to the DIV PFC PHTS which is characterised by a minimal system inertia, six Release Valves (RVs) designed in pairs to accommodate different pressure setpoints allow the fluid to pass through a defined pipework called Relief Lines (RLs) and located in the VS. The vapour flow reaches and crosses a system

of HXs submerged in pool water so that it condenses and is then subsequently channelled into the collection tanks.

In the event of large-scale discharges, which may be due, for instance, to releases of coolant from BB PHTS, six Burst Disks (BDs), designed similarly to RVs, irreversibly open when the pressure inside the VV exceeds certain thresholds. During a severe release, the vapour crosses the VS and reaches the vents downcomer connected to the suppression pool, where it condenses through direct exchange with the pool.

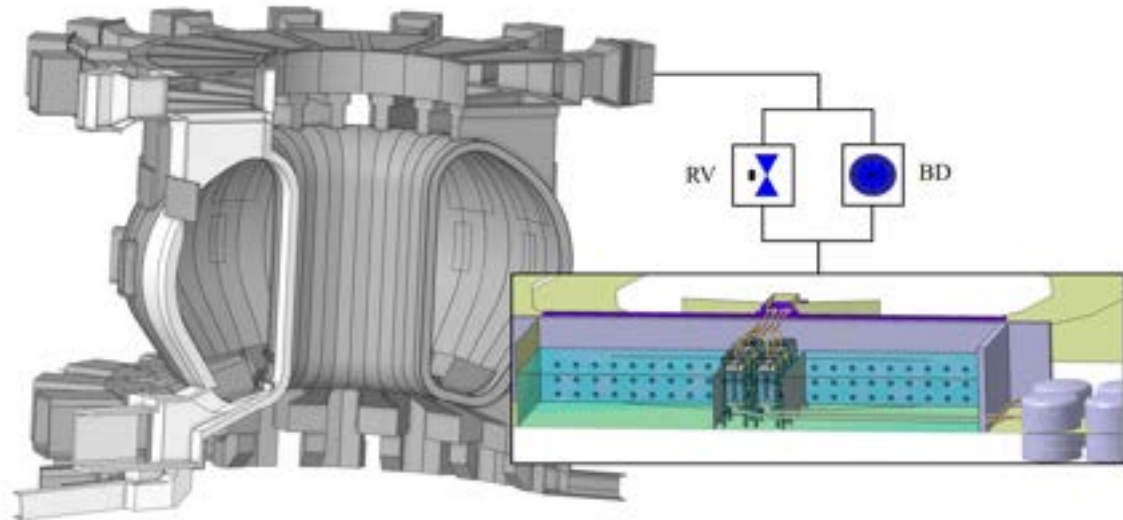


**Figure 1.23:** *Conceptual layout of VVPSS integrated with VV considering the presence of RVs and BDs.*

It is worth highlighting that, during the coolant release into the chamber, the plasma undergoes an instantaneous loss of confinement and comes into contact with the plasma-facing components, leading to the production of debris (in the form of eroded or vaporised particles), which are subsequently transported by the vapour and may be potentially radioactive. Therefore, in the case of severe discharge, although the water volume of the pool can be contaminated, the system arrangement offers significant advantages in terms of design simplicity, as well as a reduction in the number of components to be manufactured and maintained.

# Chapter 2

## Theoretical and Conceptual Framework for the Nuclear Fusion Thermal-Hydraulic Modelling

This chapter provides the theoretical and conceptual framework forming the basis of the TH System Code (SYS Code), with a particular focus on the TRACE SYS Code.

Following an overview of the evolution and architecture of SYS Code, the chapter explores, focusing on TRACE, the governing balance equations and closure relations that describe two-phase fluid behaviour, with specific attention to pre- and post-CHF flow regimes.

The role of the ECI is also discussed, highlighting its ability to couple TRACE with external computational tools and thereby extend its multiphysics capabilities.

To ensure clarity and completeness, this chapter introduces and discusses the thermo-physical properties of the materials that have been manually implemented in the TH model. These materials, not available in the default TRACE database, are essential for accurately representing the behaviour of fusion reactor components such as the PFU assembly and the PHTS piping structures.

The discussion also addresses the challenges of applying these tools to NFPPs, where the complexity of physical phenomena and the variability of operational conditions demand a more flexible and integrated modelling approach than that typically used in fission reactors.

In particular, the accurate simulation of in-vessel components such as PFCs requires advanced multidisciplinary and integrated strategies that combine TRACE with external tools. These enable a more realistic representation of heat flux distributions and the thermal-structural behaviour of components, especially under critical transient conditions such as detachment loss in the VTs. While the modelling needs have been outlined in this chapter, the specific approach adopted in this work will be further detailed in the following chapters.

## 2.1 The use of Thermal-Hydraulic system code in the field of nuclear energy

In the second half of the twentieth century, as NPPs became increasingly advanced, a need arose to assess the safety and operational efficiency of the reactor using advanced TH simulation tools capable of simulating the plant under both normal and accident conditions [43].

Early codes such as RELAP (Reactor Excursion and Leak Analysis Program) and TRAC (Transient Reactor Analysis Code) were developed in the 1970s and 1980s by institutions like the U.S. Nuclear Regulatory Commission (NRC) and national laboratories. Subsequently, these codes evolved to the development of TRACE by the NRC, which combines the strengths of its predecessors into a unified platform that supports detailed system-level simulations of light water reactors and other nuclear systems [38].

Such SYS Codes have been employed for both the design and safety analyses due to their potential to model reactor components such as pipes, pressurizers, valves and pumps. In particular, in terms of design, the TH analyses by the use of SYS Code allow:

- selecting and designing the proper plant components;
- defining and fixing the layout of the system;
- optimising the configuration of individual components;
- adapting the normal operational parameters;
- determining the expected loads.

Instead, concerning the safety aspects, the study through these tools helps to assess:

- the transient performance of NPP;
- the design/construction suitability of engineered safety features;
- the adequacy of the emergency operating/accident management procedures;
- the condition of the system to fix requirements and procedures useful in safety and licensing processes.

To date, although SYS Codes have been developed in the context of NPP, they are widely used in the fusion research field due to several advantages of the tools. Therefore, the following section will give a brief overview of the fundamentals of SYS Code, even if mostly focused on the TRACE, since it has been adopted to conduct the study reported in this dissertation.

## 2.2 Architecture of Thermal-Hydraulic system code

The TH investigation of complex systems during several conditions, including safety analysis, has been made possible thanks to SYS Codes, which have been progressively enhanced over time by the knowledge gained through the analysis of a vast amount of experiments.

The SYS Codes integrate thermal-hydraulic balance equations along with models for heat conduction, radiation, neutron kinetics, fuel thermo-mechanics, chemical reactions, and instrumentation and control systems. As such, they function as multiphysics simulation tools, incorporating various physics modules, including:

- thermal-hydraulics, conduction and radiation heat transfer modules;
- compressor, pump and turbine models;
- special system component models (pressurizer, accumulator, etc.);
- regulations, control and protection systems.

Depending on the problem or component to be investigated, a different space resolution for the flow field may be required, and for this reason SYS Codes allows modelling using three different levels of spatial discretisation, namely:

- 0D or lumped parameter models: useful in the case of low velocity components or negligible dynamic effect for which the TH main parameters, such as pressure or temperature, can be considered uniform;
- 1D models: the fluid in the generic component has a dominant flow path;
- 2D or pseudo-3D in the case of a fully velocity field.

The numerical solution scheme differs among SYS Codes, as they can employ either finite-volume or finite-difference methods based on the Implicit Continuous-fluid Eulerian techniques. The main difference among the tools does not concern the spatial discretisation scheme, but rather the time discretisation method adopted, which may be semi-implicit or fully implicit. Semi-implicit schemes require adherence to the material Courant condition to maintain numerical stability, whereas fully implicit methods can operate with significantly larger time steps without such constraints [43].

Specifically to TRACE, governing two-phase flow and heat transfer are addressed using finite volume-based numerical techniques. Furthermore, the heat-transfer equations are evaluated using a semi-implicit time-differencing technique. The fluid-dynamics equations in the spatial one-dimensional (1D) and three-dimensional (3D) components use, by default, a multi-step time-differencing procedure that allows the material Courant-limit condition to be exceeded.

## 2.3 TRACE overview

The Best Estimate system code TRAC/RELAP Advanced Computational Engine (TRACE) [38] [44] [45] has been developed for the thermal-hydraulic analysis of accidental scenarios such as LOCA or other operational transients of interest in light water reactors. The figure 2.1 illustrates the process to be generally adopted when creating an input model using TRACE.

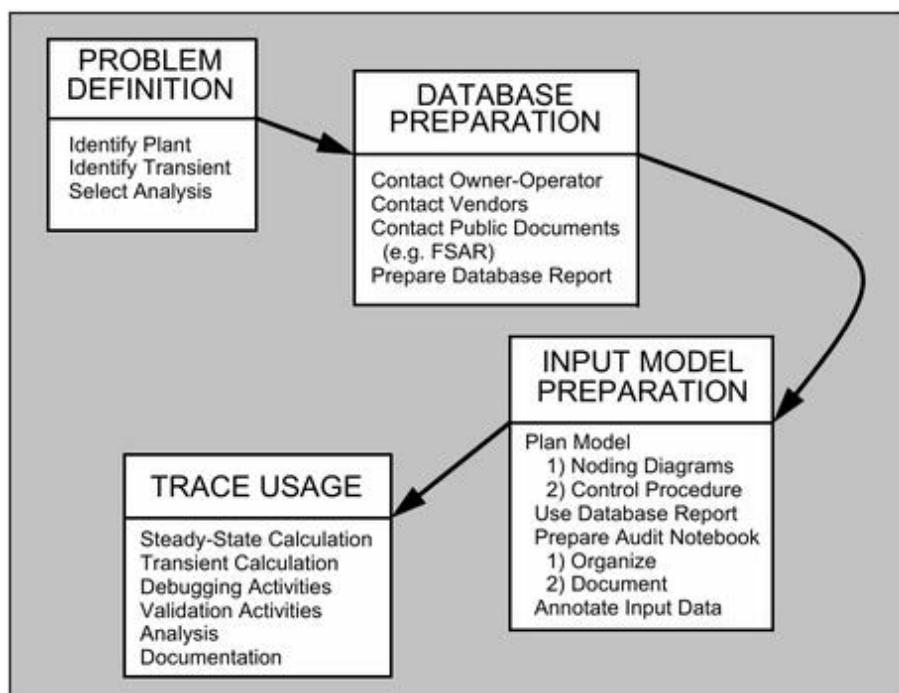


**Figure 2.1:** Workflow of Problem Solving with the TRACE [45].

TRACE enables the modelling of a nuclear facility through the use of individual components appropriately connected using a one-dimensional approach, with the option of three-dimensional modelling in the case of the reactor pressure vessel of fission plants.

The various elements of the facility circuit to be represented can be modeled using different types of components (e.g. pipe, pumps, pressurizer), which can in turn be subdivided into a defined number of volumes which are called cells within which the physical properties of the fluid are treated as average values, and the governing equations are solved in terms of their mean quantities.

The code execution times depend on the performance of the machine used for developing the analyses, the total number of cells used for the nodalisation of the system, the chosen time discretisation, and the phenomena occurring during the analysis.

TRACE can be used through the Symbolic Nuclear Analysis Package (SNAP) [46], which is a graphical interface that allows the user to develop and view the

input deck, with the additional possibility of visualising animations related to the simulations carried out (figure 2.2).

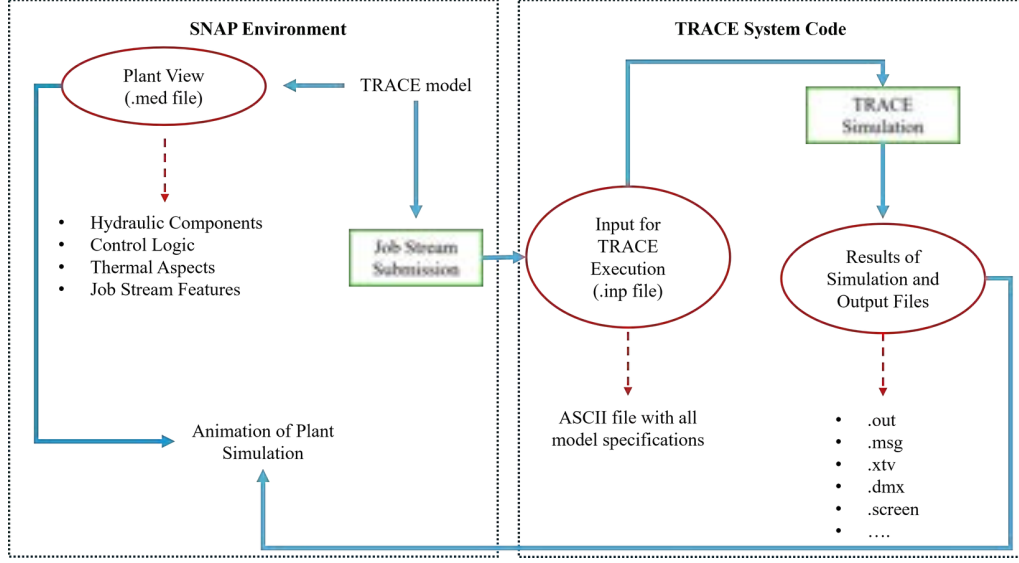


**Figure 2.2:** SNAP/TRACE workflow: from model definition to simulation output.

### 2.3.1 TRACE theory - Balance and Constitutive Equations

The basic two-fluid, two-phase field equation set solved in TRACE consists of separate mass, energy, and momentum conservations for the liquid and gas fields. The time averaged conservation equations of the k-phase are summarised in equations (2.1) to (2.3), considering k either vapour (g) or liquid (f).

$$\frac{\partial}{\partial t} (\alpha_k \bar{\rho}_k) + \nabla \cdot [\alpha_k \bar{\rho}_k \bar{\mathbf{V}}_k] = \bar{\Gamma}_k \quad (2.1)$$

$$\begin{aligned} \frac{\partial}{\partial t} \left[ \alpha_k \bar{\rho}_k \left( e_k + \frac{1}{2} V_k^2 \right) \right] + \nabla \cdot \left[ \alpha_k \bar{\rho}_k \left( e_k + \frac{1}{2} V_k^2 \right) \mathbf{V}_k \right] = & -\nabla \cdot [\alpha_k \bar{\mathbf{q}}'_k] \\ & + \nabla \cdot [\alpha \bar{\mathbf{T}}_k \cdot \bar{\mathbf{V}}_k] + \bar{\rho}_k \bar{\mathbf{k}} \cdot \bar{\mathbf{V}}_k + \bar{E}_i + \bar{q}_{dk} \end{aligned} \quad (2.2)$$

$$\frac{\partial}{\partial t} [\alpha_k \bar{\rho}_k \bar{\mathbf{V}}_k] + \nabla \cdot [\alpha_k \bar{\rho}_k \bar{\mathbf{V}}_k \mathbf{V}_k] = \nabla \cdot [\alpha_k \bar{\mathbf{T}}_k] + \alpha_k \bar{\rho}_k \bar{\mathbf{g}} + \bar{\mathbf{M}}_i \quad (2.3)$$

The first term on the left-hand side of the equation (2.1) is the time rate of mass change for the phase, while the second term is the change in mass due to the mass entering or leaving the control volume. Instead,  $\Gamma_k$  is the phase volumetric exchange rate, and since mass sources or sinks are not considered in the equation (2.1), the following relation must be valid:  $\Gamma_g = -\Gamma_f$ . Concerning the equations (2.2) and (2.3), the terms  $\bar{E}_i$  and  $\bar{\mathbf{M}}_i$  represent the total contributions of time-averaged interface jump conditions to transfer energy and momentum, respectively. In

addition,  $\mathbf{q}'_k$  is the conductive heat flux,  $\bar{q}_{dk}$  is the direct heating (e.g. radioactive decay), and  $\bar{\mathbf{T}}$  is the full stress tensor.

Although equations (2.2) and (2.3) are consistent with Reynolds averaging and allow splitting the velocities into mean and fluctuating components, the contributions derived from the Reynolds stress, as well as most of the terms on the right-hand side of the equations (2.2) and (2.3) will be modelled with engineering correlations. Therefore, they could be revised as follows:

$$\begin{aligned} \frac{\partial \left[ \alpha \bar{\rho}_k \left( e_k + \frac{V_k^2}{2} \right) \right]}{\partial t} + \nabla \cdot \left[ \alpha \bar{\rho}_k \left( e_k + \frac{P}{\rho_k} + \frac{V_k^2}{2} \right) \bar{\mathbf{V}}_k \right] = -\nabla \cdot [\alpha \bar{\mathbf{q}}'_k] \\ + \alpha \bar{\rho}_k \mathbf{g} \cdot \bar{\mathbf{V}}_k + \Gamma h_k + W_k + q_{dk} \end{aligned} \quad (2.4)$$

$$\frac{\partial}{\partial t} [\alpha_k \bar{\rho}_k \bar{\mathbf{V}}_k] + \nabla \cdot [\alpha_k \bar{\rho}_k \bar{\mathbf{V}}_k \bar{\mathbf{V}}_k] = \nabla \cdot [\alpha_k \bar{\mathbf{R}}_k] + \alpha_k \bar{\rho}_k \mathbf{g} + \bar{\mathbf{M}}_i \quad (2.5)$$

In equation (2.4), the term  $\mathbf{q}'_k$  includes the energy flux due to turbulent diffusion. The energy associated with the mass transfer is considered by the product of the mass transfer rate and the appropriate stagnation enthalpy at the interface ( $\Gamma h_k$ ). The work done is divided into: the one due to the pressure terms in the stress tensor, and a term  $W_k$  which includes the work done by both shear stress and interfacial force terms.

In equation (2.5), pressure is also isolated from the stress tensor, and the viscous shear stress terms are combined with the Reynolds stress into  $\bar{\mathbf{R}}_k$ .

The aforementioned set of field equations is then rearranged using the finite volume method and implemented in TRACE.

Furthermore, since the reactor system is not uniquely characterised by the presence of a two-phase fluid, additional equations are required. Indeed, often solutes can be dissolved into the coolant as well as noncondensable gases as part of the vapour phase or in solution with the liquid phase.

The equations governing the two-phase fluid, solutes, and non-condensable gases, along with those describing control systems and core power, are coupled with constitutive relations to ensure the closure of the system. Among these constitutive relations are:

- the equations of state;
- the correlations for wall-fluid friction and heat transfer;
- the transport equations for mass, energy, and momentum across the liquid-vapour interface [38].

Generally, in view of future power plants and, in particular, NFPPs, the applicability of SYS Codes passes through the validity range of their implemented models, with a special focus on the closure relationships concerning the:

- interfacial drag and heat transfer;
- wall-to-fluid heat transfer;
- critical heat flux closure models;
- reflood modelling;
- wall-to-wall radiation [47].

The section 2.3.3 discusses key aspects of the closure relationships and models on which TRACE is based. A comprehensive description of the closure relations, even just for TRACE, would require much more explanation than is possible in this dissertation and can be easily explored further, just like the governing equations, through the reference manuals of the code [38, 44, 45].

### 2.3.2 Flow regime of two-phase flow involved in TRACE

Many reactor systems are designed with a primary coolant that operates under single-phase flow conditions, as in the case for the PFC PHTS, and the flow can be effectively modelled using governing equations and simple heat transfer correlations. However, this condition is not always guaranteed. For instance, during accidental transients, the liquid coolant may undergo phase change, or in the case of DEMO, the VTs may experience a loss of detachment, which causes the occurrence of the two-phase flow in the in-vessel cooling circuit. The onset of two-phase flow introduces significant complexity to the coolant hydrodynamic modelling. Nevertheless, accounting for this phenomenon is essential in SYS Codes to ensure accurate design and safety analyses.

Two-phase flows can be classified into different categories based on their characteristics and flow structure. In particular, the flow regimes are typically divided into two main groups: Pre-CHF flow, which is distinguished in Vertical, Horizontal and Inclined flows; Post-CHF flow that occurs after the CHF conditions have been exceeded, and it is generally less sensitive to the inclination of the flow channel.

The vertical Pre-CHF flow is illustrated in figure 2.3. In the initial stage, dispersed bubble flow is observed, with small bubbles scattered throughout the bulk coolant. As the void fraction increases, the bubbles merge into larger “slugs”, marking the transition to slug flow. With increasing fluid velocity, the flow structure becomes unstable: although the net flow remains upward, local flow reversals occur due to instabilities. These instabilities result from the opposing effects of gravity and shear forces acting on the thin liquid film surrounding Taylor bubbles. Slug bubbles are more prevalent in smaller pipes, while annular mist flow, where gas shear dominates over gravity, emerges at higher temperatures and flow rates.

Concerning the horizontal flow regimes (figure 2.4), although the bubbly flow regime is very similar to that of vertical flows, slightly different characteristics emerge as the velocity increases. The flow transitions to stratified, then wavy, and eventually

to plug/slug flow, where liquid sections may contain bubbles. At high speeds and temperatures, annular mist flow can also occur, dominated by gas shear.

Additionally, the figure 2.5 shows the post-CHF regime, which occurs when the wall exceeds the CHF temperature and undergoes a dryout. In this case, the coolant no longer contacts the surface. These include inverted annular, inverted slug, and dispersed flow, where liquid is surrounded by vapour or exists as droplets. The dryout and rewetting points define the beginning and end of this regime.

Such flow regimes are modelled in SYS Code through the use of flow regime maps that use parameters such as flow velocity, void fraction, and temperature to determine which flow regime is present. Once the flow regime is defined, the correct closure relationship is selected. A detailed treatment of the two-phase flow models as well as the flow regime maps used in TRACE can be found in [38].

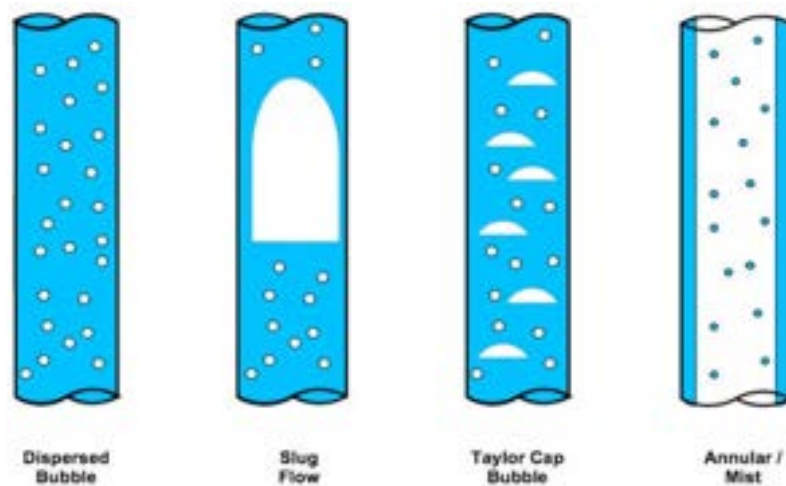


**Figure 2.3:** Vertical Pre-CHF flow regimes [38].

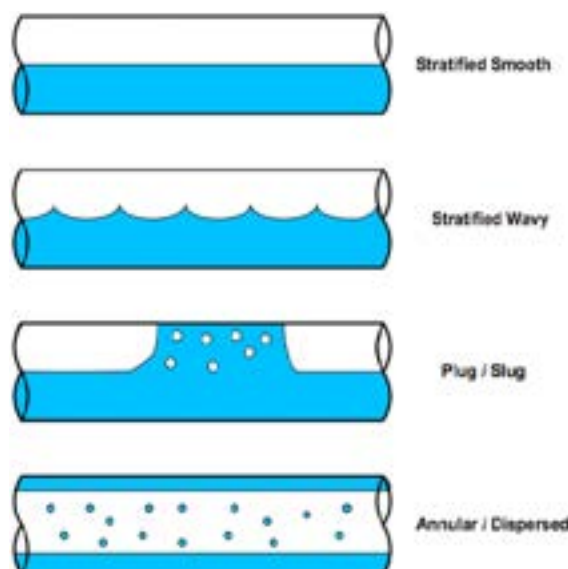


**Figure 2.4:** Horizontal Pre-CHF flow regimes [38].

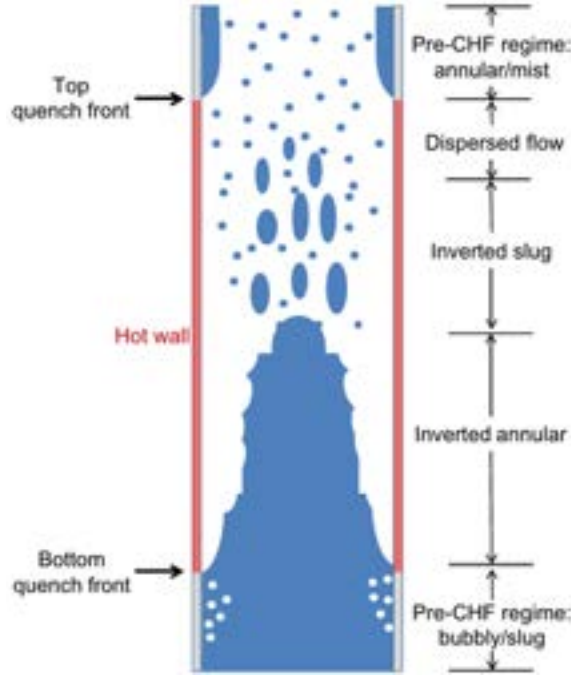


Figure 2.5: Schematic of inverted flow regimes in post-CHF [43].

### 2.3.3 Closure Relations in TRACE

In SYS Code, closure relations are essential to complete the two-phase field equation set, which loses detailed information about phase interactions, turbulence, and interfacial phenomena when they are averaged over control volumes and time. In particular, the closure models are introduced to recover this information and make the system solvable, defining key parameters such as interfacial drag, heat and mass transfer coefficients, and phase distribution, since they are crucial for accurately simulating two-phase flow behaviour.

The selection of closure relationships significantly influences the accuracy, stability, and computational performance of the simulation. Each SYS Code implements distinct sets of closure models, often tailored to specific flow regimes and geometries. These models are typically derived from experimental data and validated through separate and integral effects tests. TRACE employs a structured approach to closure modelling, particularly for pre-CHF and post-CHF flow regimes.

In pre-CHF conditions (both vertical and horizontal geometries), the interfacial drag coefficient is calculated taking into account drift velocity and profile slip factor, both of which depend on void fraction, density differences, and flow geometry. TRACE also implements dedicated models for stratified flows and post-CHF regimes, where the interfacial drag is treated differently. Further details on these models can be found in [38].

For interfacial heat transfer, TRACE implements a consistent set of correlations across flow regimes, with modifications for sub-cooled boiling and flashing. The code interpolates between regimes (e.g., bubbly to annular mist) to ensure smooth

transitions. In post-CHF conditions, the code models inverted annular and dispersed flows, using empirical correlations for heat transfer between vapour and liquid interfaces. The CHF model is based on the AECL-IPPE look-up table, with corrections for rod bundles, and defines the transition between nucleate boiling and film boiling regimes.

Furthermore, TRACE also includes models for wall-to-fluid heat transfer, radiation, and reflood conditions, although it does not implement specialised reflood correlations. Instead, it relies on adaptive meshing to capture steep thermal gradients near the quench front.

### 2.3.4 Control Procedure in TRACE

In nuclear facilities, control systems are essential for maintaining operational stability. They combine manual operator actions with automated logic to regulate variables such as pressure, temperature, and flow.

In this context, the TRACE control procedure allows the code user to exercise the same level of control over a computational model as a reactor operator would over a real reactor system. It enables both manual and automatic control of regulating hardware and can simulate abnormal hardware behaviour. For example, opening a valve connected to an atmospheric-pressure boundary condition can be used to simulate a pipe break.

Since the control procedure in TRACE is general and highly flexible, it is possible to model complex control logic, including dynamic responses to transient conditions and fault scenarios. Specifically for the PHTS, it is possible to define the control described in section 1.4.3 under normal conditions and activate emergency systems as described in section 1.6. Additionally, given the flexibility of the control system package available in the code, the transient behaviour of the plasma can be replicated under both normal and off-normal conditions.

### 2.3.5 Overview of implemented structural materials

The TRACE system code includes a built-in material library primarily developed for applications in NPPs. These libraries provide thermo-physical properties for standard materials commonly used in light water reactors, such as stainless steel, water, steam, and specific metal alloys [44].

However, when modelling fusion reactor components, particularly the PFU, it becomes necessary to incorporate materials with significantly different characteristics, which are not available in the default TRACE database.

To address this, TRACE allows the definition of user-defined materials through the use of material property tables. According to the [44], these tables enable the user to specify temperature-dependent properties for solid materials, including:

- Density ( $\rho$ )
- Specific heat capacity ( $c_p$ )
- Thermal conductivity ( $\lambda$ )

Each property can be defined as a function of temperature using tabulated data points. The code interpolates between these points during runtime to compute the appropriate values for the simulation.

This feature is essential for accurately representing the thermal behaviour of materials that are critical in fusion applications but not natively supported by TRACE.

In this work, the materials implemented include those composing the PFU assembly and the structural components of the PHTS. Their thermo-physical properties have been derived from validated sources and introduced into the model via user-defined material tables.

Specifically, the properties of the PFU layers have been retrieved from [48], while the piping material, namely EUROFER97, has been sourced from [49].

### 2.3.6 Limits of TRACE in fusion applications: the sweeping case

The challenges associated with adopting the SYS Code to simulate the behaviour of NFPP are not only related to the use of special closure relationships but also to critical modelling aspects, both physical and geometrical.

In particular, contrary to NPP, the thermal loads provided by the plasma can have a behaviour difficult to replicate within the code, being highly dynamic, spatially non-uniform, and often transient. These characteristics make it difficult to reproduce plasma-wall interactions within the code environment, especially in scenarios such as the loss of detachment in the VTs of the PFCs. In such cases, the sweeping strategy, used to mitigate peak heat fluxes, requires a modelling approach capable of capturing rapid variations in both space and time (section 1.3.4).

To overcome the limitations in reproducing plasma-induced heat fluxes, different modelling approaches can be integrated with SYS Code, depending on the required level of accuracy. For simplified representations, a dedicated control logic system can be implemented directly within the code. For more refined modelling, external Python-based tools can be employed to reconstruct realistic heat flux distributions, such as the Eich profile, and apply them as boundary conditions on thermal structures via the ECI interface, which will be described in detail in section 2.4.

In addition to these physical complexities, geometric limitations also arise. The W-MBs present a 3-D geometry that, under specific operating conditions, can influence their thermal response in ways that are not easily captured. However,

TRACE allows only for the definition of quasi-2D thermal structures. This constraint makes it challenging to define both the heat flux boundary condition and the thermal structure that simulates the PFU assembly.

To address these limitations, it becomes essential to develop equivalent thermal models that approximate the expected response of the W-MB under different operational scenarios. The present dissertation presents the modelling strategies developed and refined to improve the representation of the thermal behaviour of the VTs within the constraints of the TRACE environment.

## 2.4 Exterior Communications Interface

The NRC provides, alongside the TRACE system code, a specialised tool designed to facilitate the extension of SYS Code capabilities. This interface, known as ECI, was developed to enable the integration of TRACE with external computational tools, thereby supporting advanced multi-physics simulations. These include, but are not limited to, detailed containment modelling, three-dimensional neutron kinetics, and the simulation of complex control systems [50].

ECI permits tight coupling of results from TRACE with the results from other special codes (even itself figure 2.6) through a well-defined interprocess communication interface. This architecture allows for the synchronised exchange of data during runtime, ensuring consistency and accuracy across coupled simulations. Each separate running instance of TRACE or a coupled computer program is referred to as a “process” or “task”.

The ECI interface offers several useful applications in the context of coupled simulations:

- parallelisation of TRACE models: it allows the communication between multiple TRACE instances, which can be the set of TRACE sub-models derived from a large model;
- extension of physical modelling: ECI allows the integration of external codes to incorporate physical phenomena that are not adequately represented within TRACE;
- implementation of advanced control systems: complex control logic can be implemented externally or that existing within a TRACE model can be overridden;
- data extraction and post-processing: it also facilitates custom calculations that would be useful for analysis;

Technically, the tool consists of a collection of Fortran and C subroutines, which serve as a communication layer between TRACE and external clients. To enhance usability and flexibility, the NRC has also developed a Python-based ECI library, allowing users to implement ECI clients using Python scripts. This approach

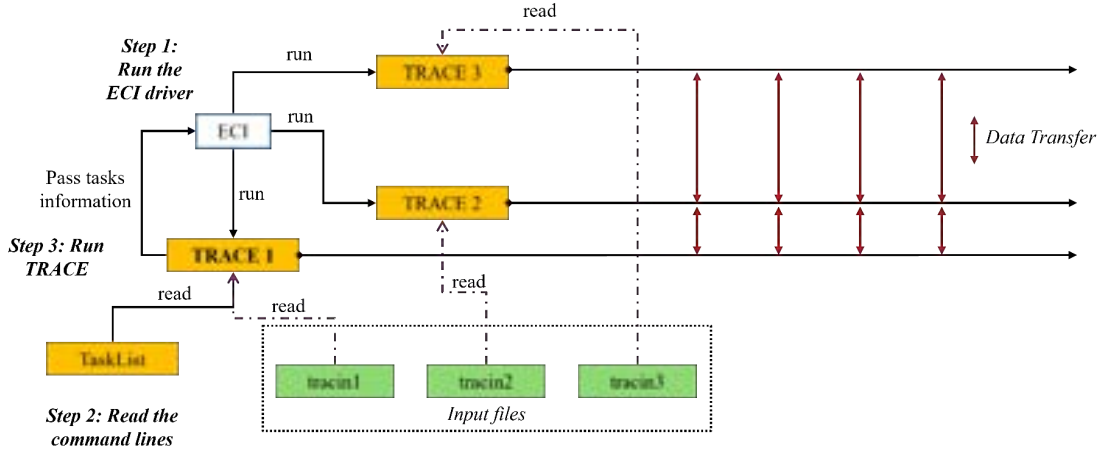


**Figure 2.6:** Parallel Execution of TRACE processes via ECI Driver.

significantly lowers the barrier to entry for developing custom coupling schemes and integrating TRACE into broader simulation frameworks.

However, this implementation is not without limitations. Notably, the official Python ECI library was compiled for compatibility with Python 2.7.x on Windows, a version that has been officially discontinued since 2014. Furthermore, the current version of TRACE that can be used is Version 5 Patch 5.

Running a coupled simulation using ECI requires an auxiliary program called the ECI Driver, which is a Java application that must be launched independently and operates as a background (or optionally foreground) process. Its primary role is to spawn child processes and manage the setup of interprocess communication among all components involved in the simulation. Importantly, the driver itself does not participate in the simulation.

Once the driver is running, the simulation is initiated by starting TRACE or another ECI compatible program. This process is designated as the central task, and the remaining processes, referred to as satellite processes, are automatically launched by the driver.

The driver operates through an infinite loop in which it waits for connections, exchanges data, launches applications, and returns to the waiting state. Generally, all communication between the driver and processes occurs via sockets.

The specific arrangement of processes used to execute a simulation is referred to as the process topology. In an ECI-based simulation, this topology is defined by a configuration file known as the taskList. In the case of multi-task system simulation, a file containing the list of individual tasks that will be started, including the central process, must be defined.

The file consists of pairs of task descriptor lines, interspersed with any number of blank or comment lines [50]. Each task descriptor pair defines a single task: the first line specifies a unique name for the task, the path to the executable program, and, if necessary, any command-line arguments. The second line indicates the hostname

where the program will be executed and the working directory for that task. A sample taskList file is given below figure 2.7.

```
# Task Name      Program Name  arguments
trac1           ./trace
# Host Name      Working Directory
first-host      /u0/analysis/MyJob/trac1/

# Task Name      Program Name      arguments
trac2           c:\Python27\python.exe  myscript.py
# Host Name      Working Directory
second-host     /u0/analysis/MyJob/trac2/
```

**Figure 2.7:** Example of taskList in ECI-based simulation.

Additionally, in ECI-based simulation, a Python script can be an ECI process as well as TRACE itself. The script can be used to post-process the TRACE results collected during the simulation and/or to modify TRACE variables inside the model, such as the control blocks or input variable of the TRACE component.

Furthermore, Python scripts effectively act as a client that helps to connect to the driver, sending and receiving data, and handling synchronisation and communication logic. The Python ECI client must have implemented the following functions that are available in the Python ECI library:

- `ProcessArgs()`: processes the ECI command-line arguments.
- `SetTasks()`: establishes communication with the ECI server.
- `ResolveComponents()`: requests the variables from the ECI client.
- `SetExTransfers()`: organises the information required to transfer data between ECI clients.
- `TimeEvolve()`: initiates and manages the simulation loop.

It is also possible to perform the first four functions using a single convenience function, namely `InitECI()`.

## Chapter 3

# Thermal-hydraulic modelling approach and analyses of the Plasma Facing Components under sweeping conditions

In the following chapter, the detailed modelling approach and the thermal-hydraulic analysis of the PFCs and in-vessel cooling circuit will be presented. The chapter primarily focuses on the definition of a simplified generic VT composed by a single PFUs assembly and compared with a reference study in order to demonstrate the validity of the approach.

The VT modelling technique is described in detail, considering all aspects associated with the model set-up, the solutions to properly simulate the dynamic response of the MB and the coolant behaviour inside the CuCrZr pipe. Additionally, the strategies used to reproduce the thermal loads on the VT surface under sweeping conditions are discussed.

The results of the thermal-hydraulic simulations are then presented, followed by a parametric analysis based on the variation of the main sweeping parameters, in order to compare the outcomes with relevant bibliographic studies.

Since the simplified aforementioned model is not relevant for DEMO baseline (both geometrically and in terms of coolant operating conditions) and is used to conduct a test bench with reference study, at a later stage, the chapter will address the same procedure by the use of the DEMO MB considering the updated thermal-hydraulic conditions. Moreover, some improvements to the procedure will be proposed in terms of the simulation of the thermal structure of the MB.

At the end, on the basis of all the improvements and procedures developed for the simplified generic VT equipped with DEMO MB, the study of a PFC in-vessel cooling circuit considering both VTs will be proposed taking into account, as further improvement, the capability to reproduce the sweeping of the Eich equation. To this

end, the TRACE model has been coupled with Python scripts through the use of the ECI driver (section 2.4).

### 3.1 Model set-up and study of simplified generic Vertical Target

In order to reproduce correctly both TH and thermal behaviour under sweeping conditions, a TH model of a simplified generic VT has been developed.

In particular, a single PFUs assembly has been developed with a total length of 20 cm, considering the water velocity equal to 12 m/s with a pressure of 5 MPa and an inlet temperature of 200 °C. Although these operating conditions are not representative of the current DEMO DIV design, they have been selected in analogy with the reference study[30], which forms the conceptual basis of the test bench of the methodology. The objective has been to reproduce the same boundary conditions to ensure consistency with the literature and to enable a direct comparison for validating the proposed methodology. It is worth highlighting that, unlike the reference study, which conducts a parametric analysis of the sweeping application to evaluate the thermal and mechanical performance of the layers within a single MB, the developed TH model includes a number of MBs equivalent to those contained in the modelled PFU assembly. This allows for the evaluation of the thermal behaviour across multiple MBs, helping to identify the most critical sections. This aspect will be further clarified in the following subsections of the present section 3.1.

The coolant inside the simplified VT (see figure 3.1) is reproduced in TRACE as a pipe consisting of 13 hydraulic cells, whose hydraulic diameter has been defined considering the presence of the ST. Additionally, the heated diameter has been calculated in order to predict with reasonable accuracy the heat transfer at the fluid-wall interface within the tube equipped with the ST.

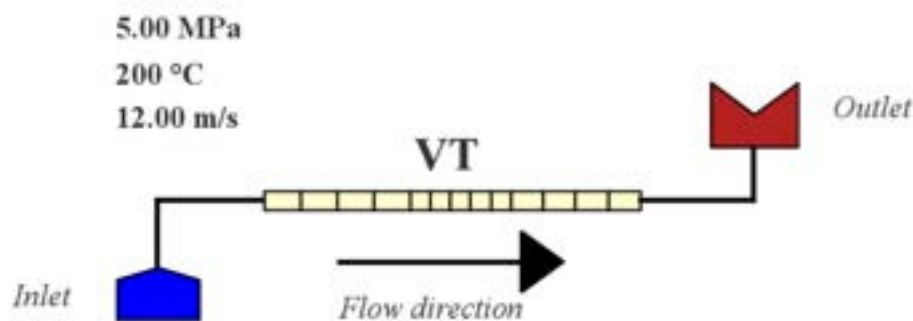


**Figure 3.1:** TRACE view of the simplified generic VT.

The axial length of each hydraulic cell has been defined in order to preserve the thermal power on the VT and will be discussed in detail in the subsection referring to the thermal model of the PFU assembly.

The concentrated hydraulic resistances occurring within the flow domain are not considered in this model, since the purpose is to better characterise the thermal aspects related to the materials and the coolant in the case of loss of detachment and the subsequent application of the sweeping mitigation strategy. A hydraulic wall roughness has nevertheless been set, according to [17].

As follows, the thermal and control system sub-models will be treated in detail. In particular, the thermal sub-model is properly defined in order to simulate the heat transfer phenomena across the equivalent MBs, while the control system sub-model comprises the complex architecture of control logic able to replicate the sweeping strategy.

The thermo-physical properties of Cu and CuCrZr relevant to the problem (specific heat capacity and thermal conductivity) have been defined as temperature-dependent (section 2.3.5), whereas the density has been kept constant. This approximation has been adopted because the variation of density with temperature within the considered range has been negligible compared to its effect on thermal behaviour. Furthermore, accounting for density variation would have required adjusting the material volume to keep the mass constant, which has not been feasible in TRACE since the geometry of the heat structure has to be predefined in SNAP interface. Regarding W, the properties have been distinctly defined to replicate the dynamic behaviour of MB, as discussed in the following section 3.1.1.2.

### **3.1.1 Thermal sub-model of simplified generic Vertical Target**

Since TRACE only allows for cylindrical, spherical or slab-type heat structures, it has been deemed necessary to develop a specific modelling approach to simulate the dynamic response of the MB during the application of the sweeping technique.

Specifically, it has been decided to replace the 3D geometry of the MB with a cylindrical structure so that each cylindrical layer of material could accurately reproduce the behaviour of MBs and the PFUs assembly in general.

Generally, it is possible to observe that only a portion of the W-MB together with layers of Cu-CuCrZr actively contributes to the conduction of thermal heat flux due to its direct contact with the exhausts. Therefore, the geometry can be distinguished into two regions:

- front side: the part located near the plasma facing surface, where a temperature gradient is visible as a result of the heat flux from the plasma. In this region, a volumetric heat source must also be considered due to neutron interactions with the structures;
- back side: the part where the thermal power is primarily due to neutron interactions with the remaining volume of the solid geometry;

Below, to reproduce the thermal results of [30], only the front part is considered, as it represents the effective region impacted by the swept heat flux. Furthermore, as in [30], the volumetric source associated with the neutrons' interaction has been neglected. A detailed account of the thermal equivalent heat structure is provided in section 3.3.1, where the overall single in-vessel cooling circuit of the PFCs is analysed.

Therefore, the corresponding TRACE heat structure of the front side is a multilayer cylindrical geometry which envelopes the hydraulic pipe. Each hydraulic cell has a corresponding thermal cell with which the coolant interacts.

In analogy with [17], a CHF multiplier of 1.15 is applied to account for the limitations of TRACE in predicting the CHF in pipes equipped with STs, as it adopts the AECL CHF look-up table [51], valid for pipes without any heat transfer enhancers.

The thermal modelling of the equivalent heat structure has required dedicated strategies in terms of axial and radial discretisation, as detailed in the following sub-sections.

#### 3.1.1.1 Axial discretisation of the generic Vertical Target

At first, the axial thermal discretisation has been closely linked to the hydraulic one by enforcing a direct correspondence between each thermal and hydraulic cell. As a result, the axial discretisation has been constrained both by hydraulic considerations and by the need to preserve the power within the generic VT. This optimisation of the discretisation has been carried out using a dedicated MATLAB algorithm [52], specifically developed for this purpose, previously adopted in [17].

Therefore, the 13 thermal cells have been defined based on the following constraints:

- a minimum axial length larger than or equal to 1 cm, to ensure numerical stability and compatibility with the fluid field resolution schemes adopted by TRACE, allowing for wider margins in the simulation time step and thereby improving computational efficiency;
- the total number of cells at most equal to 13 in order not to overcomplicate the modelling (section 3.1.2);
- the need to capture the peak of the heat flux profile;
- the need to preserve the linear heat flux (MW/m) applied to the VT.

Specifically to the last point, the equation (3.1) has been imposed, where  $\Delta x_i$  is the axial length of the generic i-cell,  $q_i''$  the heat flux at the i-centroid and  $q_{lin,VT}$  the linear heat flux obtained by integration across the VT axial length of the heat flux profile in case of loss of the detachment.

$$\sum_{i=1}^{N_{TOT,cell}} q_i'' \cdot \Delta x_i \approx q_{lin,VT} \quad (3.1)$$

It is worth noting that, at first, having set an axial length greater than the thickness of the MB, the corresponding thermal cell does not fully represent the actual MB. Nevertheless, the use of averaged heat fluxes and the assurance of linear flux conservation allow, as shown in section 3.3.4, the modelling to remain consistent with the reference case study. Moreover, to prevent thermal exchange between the thermal cells along the axial direction, the axial conduction has not been set.

In the first instance, in analogy with [30, 13], the normalised heat flux profile (equation (3.2)) has been assumed to be described by a simplified Gaussian instead of the Eich profile in attached conditions, as shown in figure 3.2a. In particular, equation (3.2) described the power density distribution at the initial time when the detachment is lost in the x-axial spatial coordinate. The multiplicative term  $a$  is the maximum value of the distribution, i.e. the peak heat flux; this value therefore varies considering different peak scenarios as illustrated (figure 3.2b).

$$q''(x) \approx a \cdot \exp \left( - \left( \frac{(x - b)}{c} \right)^2 \right) \quad (3.2)$$

This aforementioned coefficient defines the peak value of the Gaussian curve and corresponds to the highest thermal load experienced on the target. The term  $b$  is the location of the peak; it corresponds to the point of maximum plasma impact on the VT, which is not the SP. Furthermore, the  $c$  value is the width parameter of the Gaussian and is derived through the interpolation of the footprint profile [30].

Therefore, in accordance with the application of the sweeping mitigation strategy in case of loss of detachment, a Gaussian normalised profile (equation (3.3)) has been defined across the axial direction  $x$  of the VT, considering the sweeping of the SP here shown by the term  $x_{SP}(t)$ . The sweeping can therefore be represented by a sinusoidal function (equation (3.4)), where  $A$  is the amplitude, and the angular velocity is connected to the frequency  $\omega = 2 \cdot \pi \cdot f$ . The phase  $\phi$  is defined as in equation (3.5), based on the initial position of the SP.

$$q''(x, t) = a \cdot \exp \left( - \left( \frac{(x - b - x_{SP}(t))}{c} \right)^2 \right) \quad (3.3)$$

$$x_{SP}(t) = A \cos(\omega t + \phi) \quad (3.4)$$

$$\phi = \cos^{-1} \left( \frac{x_{SP}(0)}{A} \right) \quad (3.5)$$

Once the VT has been properly discretised, the heat flux profile of the centroids

considering the sweeping of the curve has been calculated. In particular, for each thermal cell, the heat flux profile associated with a single period has been defined as a tabulated input to be provided to the TRACE model (section 3.1.2).

Figure 3.3 illustrates the heat flux trend across all thermal cells under a specific sweeping condition (amplitude of 50 mm and frequency of 1 Hz) with a peak of 15 MW/m<sup>2</sup>. It is possible to note from the figure 3.3 that there are cells subjected to the highest thermal load in a wider time range adjacent to the edges of the sweeping span, i.e. “Cell 05” and “Cell 10”. Consequently, under these conditions, the cells that require the most careful monitoring are those positioned at the mid-span of the sweep, starting from the peak of the heat flux trend associated with the detachment loss event.

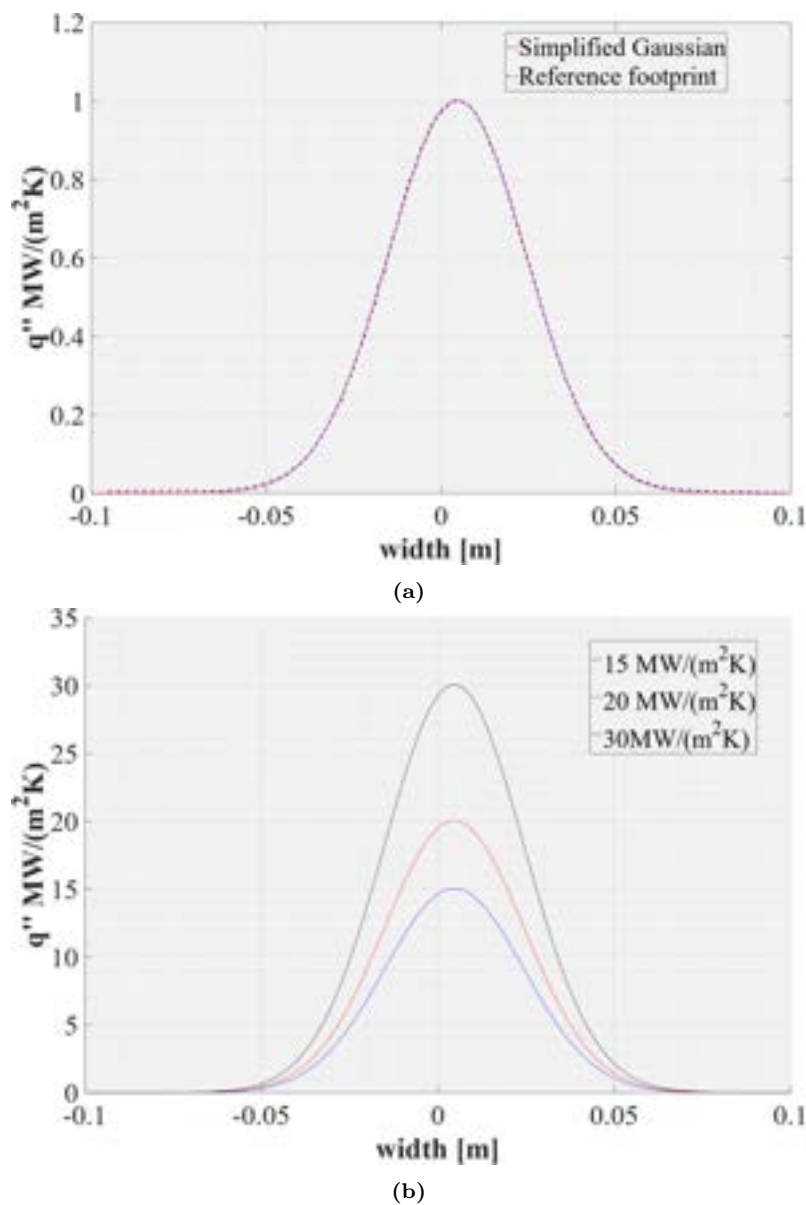


**Figure 3.2:** Heat flux profile comparison and peak variation in attached condition: (a) comparison between footprint [30] and normalised profile, (b) simplified Gaussian trend at different peak values.

As shown in figure 3.4, increasing the sweeping amplitude significantly modifies the time-averaged heat flux distribution. When the amplitude is zero ( $A=0$  mm), the profile exhibits a sharp central peak of about  $15 \text{ MW/m}^2$ . As the amplitude increases (from 50 to 200 mm), this peak progressively decreases and the distribution becomes flatter and wider. At the same time, part of the heat flux is redistributed toward the edges of the sweeping range, resulting in maxima near the amplitude extremes. Conversely, for a fixed amplitude, the axial distribution of the time-averaged heat flux remains essentially independent of the sweeping frequency.

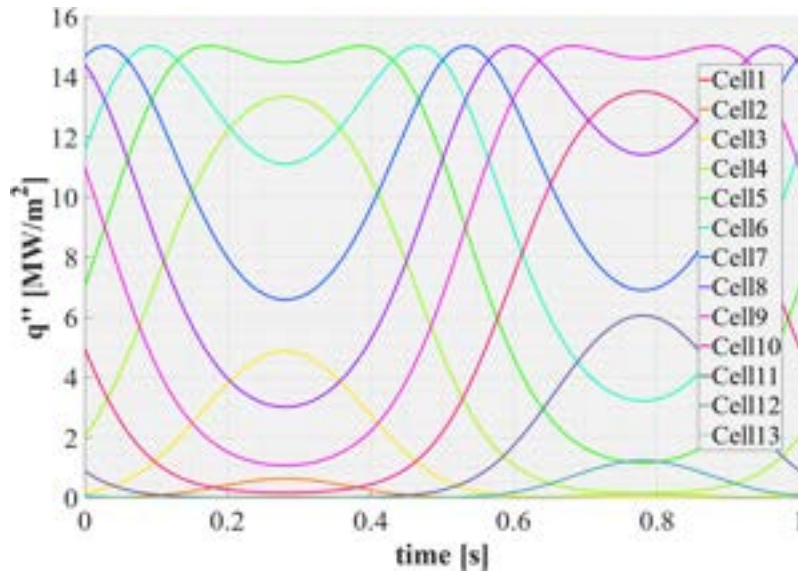


**Figure 3.3:** Temporal evolution of heat flux in thermal cells for a given sweeping scenario.

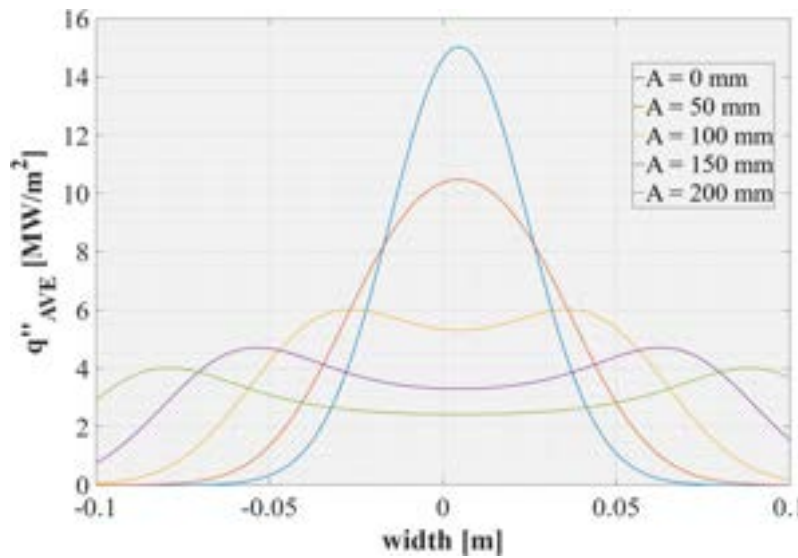


**Figure 3.4:** Axial distribution of the average heat flux over time, considering a fixed sweeping frequency of 1 Hz.

### 3.1.1.2 Radial discretisation of the generic Vertical Target: the optimisation procedure

To accurately represent the radial shape of the equivalent thermal structure of the MB, a dedicated optimisation procedure has been developed. Its goal is to preserve the thermal inertia of the system while capturing the maximum temperature reached by the materials during transient conditions.

This procedure is based on a multi-objective optimisation algorithm, specifically designed to generate an equivalent cylindrical W layer. The optimisation simultaneously defines:

- the geometric parameters of the thermal structure,
- the effective thermodynamic and transport properties of the materials.

The approach aims to reproduce the dynamic thermal behaviour of the real 3D MB under sweeping conditions, which is particularly challenging to replicate in TRACE.

In earlier approaches, the discretisation of the MB has been carried out by replacing the full 3D geometry [17] with an equivalent heat structure designed to preserve the solid domain volume and the plasma facing surface, thereby maintaining the energy content of the MB (figure 3.5).

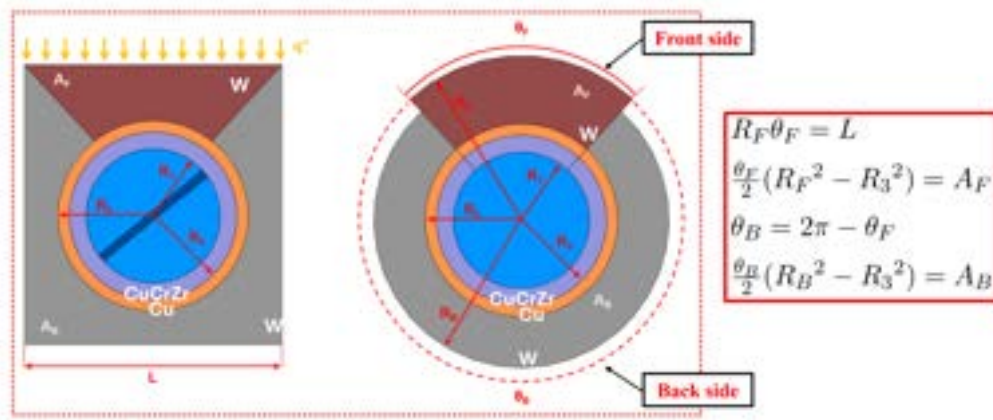


**Figure 3.5:** Definition of an equivalent thermal structure based on volume conservation [17].

Although the method has yielded more than acceptable results under steady-state conditions or operational transients associated with the common duty cycle of the fusion device, its application to sweeping scenarios reveals some limitations. In particular, the equivalent MB derived from the previous approach does not fully capture two key transient parameters (section 1.3.4):

- the maximum temperature of the materials, which is a key indicator of potential structural failure of VTs. These values are fundamental to assess the structural integrity of the component and to ensure the long-term viability of the DIV;

- the wall heat flux to the fluid, which is essential for assessing the risk of reaching CHF conditions. Indeed, the CHF represents the threshold beyond which the heat transfer efficiency of the cooling system drastically deteriorates, potentially leading to local overheating, material degradation, and compromised thermal management of DIV.

Given these aspects, the multi-parametric optimisation procedure has been set up, defining the following objectives:

- capture maximum W temperature evolution in time;
- capture maximum Cu temperature evolution in time;
- capture maximum CuCrZr temperature evolution in time;
- have the same maximum heat flux at the coolant side;
- preserve the total energy content.

In particular, preserving the total energy content is essential to ensure thermal consistency and physical validity of the equivalent thermal structure with respect to the real geometry. This enables the model to reproduce the thermal inertia of the original 3D-MB, particularly during transient events such as SP sweeping. By maintaining the energy balance, the equivalent structure can accurately follow the temperature evolution and dynamic behaviour of the real component, avoiding phase shifts and ensuring reliable simulation of time-dependent thermal phenomena.

The adoption of the optimiser guarantees a generalisable approach due to the possibility of easily changing the objectives of the problem and the target. This is made explicit in the case of the MB problem, by the choice of defining as target set only the energy content, just the compliance with the maximum temperature of materials, or both. Furthermore, the use of the optimiser has the advantage of weighing the goals, giving a certain emphasis to each target of the selected set.

The optimised geometry has been retrieved by the use of a structured workflow (see figure 3.6) which consists of a multi-step process in order to simulate and validate the equivalent MB adopted in TRACE model of VT.

As shown in figure 3.6, the first step of the workflow to develop the optimisation procedure involved referencing the literature study [30], although it is essential to underline that the methodology will be applied for any type of MB whose the TH data and geometry aspects are known. This reference activity served as the foundation to develop and compare an ANSYS-CFX [53] model of the real 3D-MB, whose thermal response has been compared with the same reference [30]. Subsequently, the ANSYS-CFX model has been adopted to study the thermal response of the MB considering two step-increases in heat flux. From the simulations, it has been possible to retrieve the target value on which the equivalent MB has been optimised.

The second step, carried out entirely in MATLAB, has consisted of the definition of the optimised thermal structure. This has been achieved by employing an input

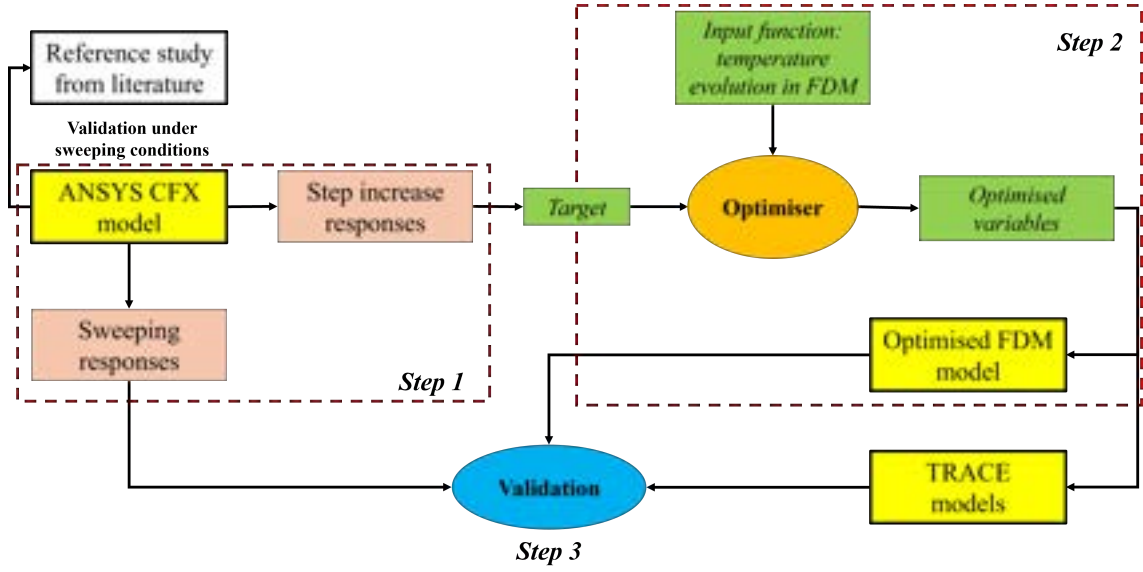


**Figure 3.6:** Structured workflow optimised equivalent MB.

function that represented the temperature evolution over time within a multilayer cylindrical geometry, modelled using a Finite Difference Method (FDM) model.

In the final step, the optimised thermal structure has been studied in both TRACE and developed FDM algorithm. The results have been compared to the sweeping responses of the real 3D-MB in ANSYS-CFX, allowing for validation of the optimised geometry and confirming its accuracy and reliability.

A comprehensive, step-by-step workflow description is provided as follows.

Starting from the first step, the ANSYS-CFX model takes into account the TH data and geometry aspects of the literature study [30], which are shown in tables 3.1 and 3.2. Since it is a purely thermal analysis, the fluid has not been explicitly modelled and has been instead represented with a Robin Boundary Condition (BC), imposing a heat transfer coefficient and a bulk coolant temperature.

**Table 3.1:** Geometry data of the MB.

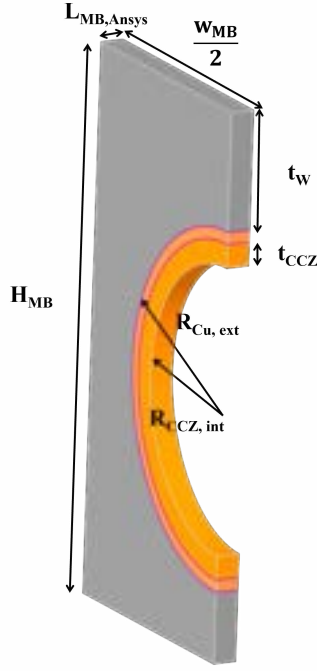
|                        | Dimension [mm] |
|------------------------|----------------|
| $\mathbf{H}_{MB}$      | 23             |
| $\mathbf{W}_{MB}$      | 22             |
| $\mathbf{L}_{MB}$      | 4              |
| $\mathbf{R}_{CCZ,Int}$ | 6              |
| $\mathbf{t}_{CCZ}$     | 1              |
| $\mathbf{t}_{Cu}$      | 0.5            |
| $\mathbf{t}_W$         | 5              |

The investigated domain (figure 3.7) corresponds to half of a real 3D-MB, in which the three solid layers have been modelled using the shared topology feature, ensuring continuous mesh connectivity and physical interaction across the interfaces. Furthermore, due to the invariance of the thermal field along the axial direction, the

**Table 3.2:** *TH input parameter.*

|                                                  | Value                    |
|--------------------------------------------------|--------------------------|
| $T_{W,initial}$ [ $^{\circ}\text{C}$ ]           | 200                      |
| $T_{water}$ [ $^{\circ}\text{C}$ ]               | 200                      |
| $h_{water}$ [ $\text{kW}/(\text{m}^2\text{K})$ ] | Variable with $T_{wall}$ |

problem can be accurately represented as a 2D thermal model. Consequently, the  $L_{MB}$  has been neglected and replaced with a fictitious thickness ( $L_{MB,Ansys}$ ) equal to the mesh element size, to reduce the computational time.

**Figure 3.7:** *Geometry of the 3D-MB under investigation.*

The resulting mesh, given the bounded domain, has included a total number of nodes and elements respectively equal to 149352 and 73937, with a predominance of hexahedral elements.

Each material property relevant to the problem (specific heat capacity and thermal conductivity) has been defined as a function of temperature [48, 54, 55], except for density, which has been kept constant based on the observation discussed in section 3.1. A Neumann heat flux BC has been imposed on the external surface of the W-MB in the plasma facing side. Meanwhile, a temperature-dependent curve has been defined to reproduce the Heat Transfer Coefficient (HTC) trend at the wall, derived from the MATLAB routine of [56].

In a preliminary stage, the ANSYS-CFX model of the 3D-MB, also referred to in the following sections as single PFU, has been studied considering the application of the sweeping technique to ensure it is in line with the reference one. The analysis has been conducted considering two cases: a constant heat flux applied to the MB

with a peak of  $15 \text{ MW/m}^2$  and a swept heat flux with the same peak characterised by an amplitude of 5 cm and a frequency of 1 Hz.

The corresponding thermal field obtained under the stationary heat flux is reported in figure 3.8 and compared with the reference study reported in the red box on the left.

Furthermore, figure 3.9 shows the comparison of the swept heat flux in the outer surface of W, which is exposed directly to the plasma and the maximum temperature at the W domain between the reference study (left) and the developed model (right). Given that the trends of both the input and output parameters are significantly in line with the reference case study, the ANSYS-CFX model developed has been subsequently adopted to define the target parameters required by the optimisation procedure.

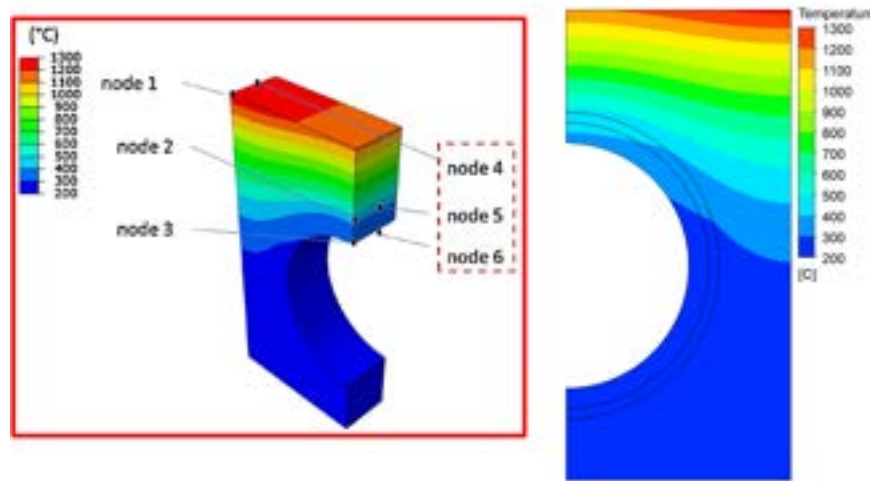


**Figure 3.8:** Comparison of the temperature distribution in MB between the reference study [30] (left) and the developed ANSYS-CFX model (right).

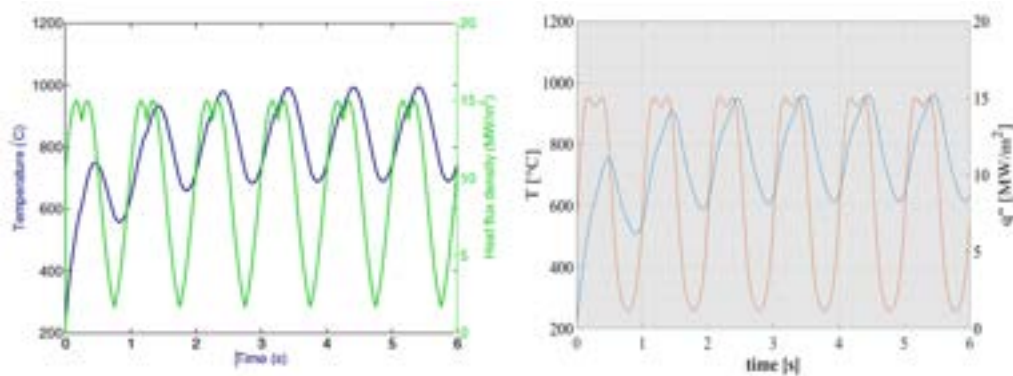


**Figure 3.9:** Comparison heat flux and maximum temperature at W material: reference study (left) and developed ANSYS-CFX model (right).

The simulations have allowed defining the peaking factor equation (3.6), useful

to assess the constraint associated with the CHF margin (section 1.3.4) essential in the definition of the optimised heat structure.

$$f_p = \frac{q''_{w,max}}{q''_0} \quad (3.6)$$

Therefore, in line with step 2 of figure 3.6, an optimiser has been adopted, in particular, a multi-objective genetic algorithm (GAMULTIOBJ) implemented in MATLAB [52] has been used. The GAMULTIOBJ is based on a controlled, elitist genetic algorithm, which is a variant of the NSGA-II [57]. It allows for maintaining a diverse set of solutions, avoiding the sudden convergence of the problem to a suboptimal result and preserves in each generation the best solution (elites), ensuring high-quality outcomes. Furthermore, it efficiently retrieves a set of optimal trade-offs between multiple objectives, known as Pareto efficiency.

To proceed with the optimisation, some assumptions have been made. In particular, the CuCrZr and Cu properties of the equivalent MB have been considered constant with the temperature, and no volumetric power associated with nuclear interactions has been considered. The table 3.3, in accordance with [48, 54], shows the material properties, except for the W layer, used in the optimisation problem and calculated at the fluid temperature, which is also the temperature of the materials at the initial time.

**Table 3.3:** *Assumed thermodynamic and transport properties.*

| Layer         | $c_p$ [J/(kg K)] | $\lambda$ [W/(m K)] | $\rho$ [kg/m <sup>3</sup> ] |
|---------------|------------------|---------------------|-----------------------------|
| <b>CuCrZr</b> | 358.49           | 341.40              | 8933                        |
| <b>Cu</b>     | 356.00           | 387.36              | 8933                        |

The W layer has been defined as the optimisation variable of the problem since it is the section over which the 2D thermal behaviour occurs. Specifically, the conductivity  $\lambda_W$  and the product  $\text{Log}_{10}(\rho_W c_W)$  have been defined as thermodynamic and transport properties to be optimised as well as the thickness of the cylindrical W layer able to reproduce the behaviour of the real MB. Actually, the definition of the product  $\text{Log}_{10}(\rho_W c_W)$  it is a stratagem to find the  $c_W$  optimised value in a narrow range and the  $\rho_W$  is set to be 19297 kg/m<sup>3</sup> according to [55].

It is important to emphasise that the assumption of constant thermo-physical properties has been necessary to avoid increasing the complexity of the optimisation procedure. This will become clearer when the input function useful in the optimisation procedure is introduced. Furthermore, with regard to density, the same observation discussed in section 3.1 remains valid.

The GAMULTIOBJ has required the definition of intervals within which to search for optimums, and the table 3.4 below shows the lower and upper bounds of the selected ranges as well as the initial values to start the optimisation problem.

**Table 3.4:** *Optimised variables of the equivalent MB.*

| Variables                      | Lower Bound | Upper Bound | Initial Value |
|--------------------------------|-------------|-------------|---------------|
| $t_W$                          | 0.005       | 0.025       | 0.015         |
| $\text{Log}_{10} (\rho_W c_W)$ | 5           | 7           | 6.40          |
| $\lambda_W$                    | 100         | 250         | 150           |

Therefore, in order to define the optimised thermal structure, starting from the validated ANSYS-CFX model of the real 3D-MB, the targets have been defined by means of thermal simulations in the case of the application of a step increase in heat flux of about both 15 and 30 MW/m<sup>2</sup>. The resulted targets (figure 3.10) of optimisation for both heat fluxes have been:

- the maximum W temperature at 2.5 s from the application of thermal load;
- the maximum W temperature at 5 s from the application of thermal load;
- the maximum W temperature in the steady-state condition;
- the maximum Cu temperature in the steady-state condition.

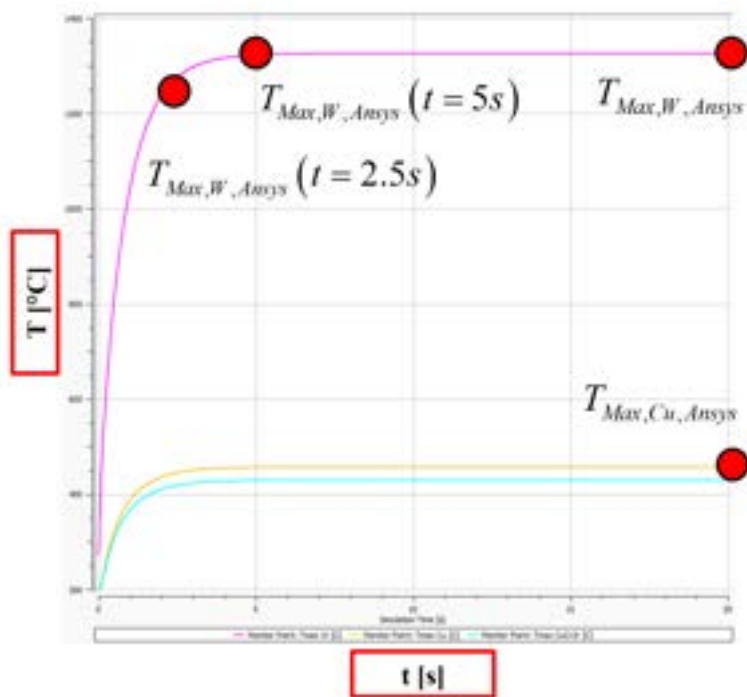
As illustrated in 3.10, the three instants, on which the targets have been retrieved, have been selected to capture the most relevant stages of the thermal response of the MB under the step-increase in heat flux. The intermediate times of 2.5 s and 5 s have been chosen to characterise the transient behaviour during the early and intermediate phases following the applied thermal load, when the temperature gradients and heat diffusion dynamics are most pronounced. Finally, the steady-state condition has been considered to catch the effective long-term thermal behaviour.

The optimiser has consequently minimised the objectives ( $Obj_i$ ) summarised in equation (3.7), where the subindex  $target, i$  represents the target value of the previous list, drawn from ANSYS-CFX calculations. In particular, the  $W_i$  is the specific weight (collected in table 3.5) associated with the single objective  $Obj_i$ . These weights have been assigned according to the applied heat flux, giving greater importance to objectives related to the highest heat flux scenario (30 MW/m<sup>2</sup>). This approach has been adopted to ensure that the equivalent 2D thermal structure reproduces the behaviour of the 3D-MB as accurately as possible under the most demanding thermal conditions. In addition, for each heat flux case, a higher weight has been attributed to the W steady-state temperature compared to the other targets. Still referring to equation (3.7), the  $X_{target, i}$  is the quantity value obtained from thermal analyses by the use of ANSYS-CFX, while  $X_{opt, i}$  is the value calculated for each optimised set by the use of an input function of the optimiser.

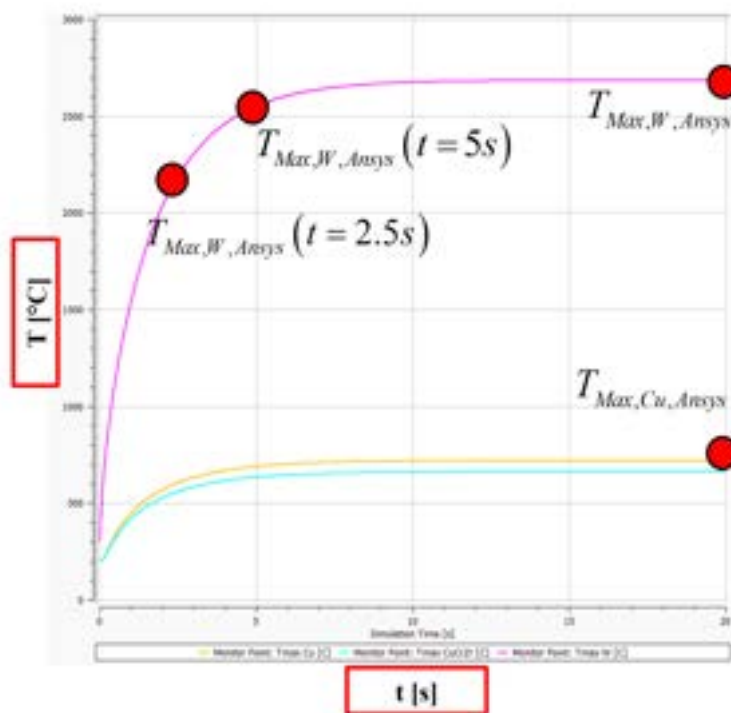
$$Obj_i = \min \left( W_i \frac{abs(X_{opt, i} - X_{target, i})}{X_{target, i}} \right) \quad (3.7)$$

Focusing on the input function, which allows the calculation of the  $X_{opt, i}$ ,

it essentially corresponds to the time assessment within the equivalent thermal structure of the thermal field.



(a)



(b)

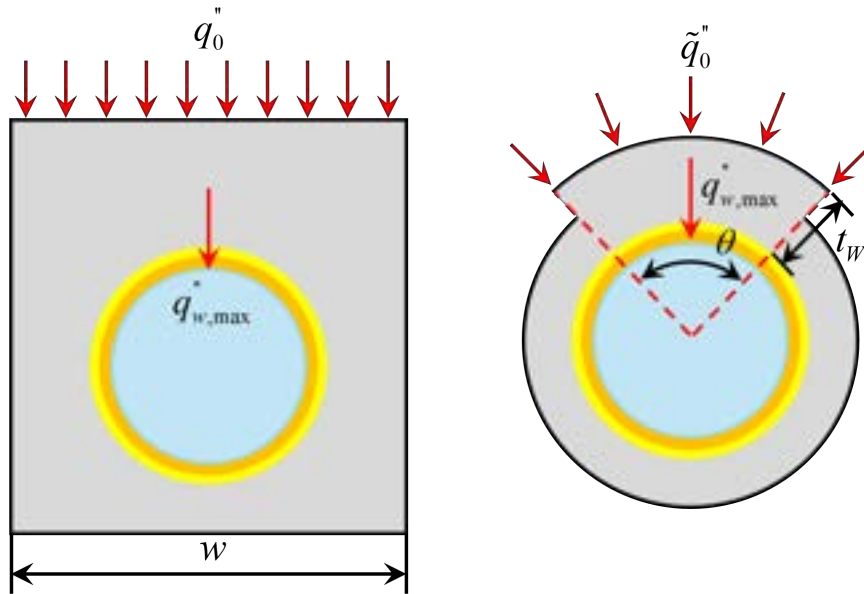
**Figure 3.10:** ANSYS-CFX monitor points for defining targets under step heat flux increases: (a) 15, (b) 30 MW/m<sup>2</sup>.

**Table 3.5:** *Weights of the targets.*

| $q''$ [MW/m <sup>2</sup> ] | 15 | 30 |
|----------------------------|----|----|
| $W_1$                      | 1  | 2  |
| $W_2$                      | 1  | 2  |
| $W_3$                      | 2  | 4  |
| $W_4$                      | 1  | 2  |

This function has been developed using a dedicated model in which the temperature evolution in cylindrical geometry is solved through FDM across the radial direction, taking into account the CHF margin and preserving the power.

A sketch comparing the 3D-MB and equivalent structure is depicted in figure 3.11, where it is possible to observe the geometrical variable to be optimised  $t_w$  and those constrained  $\theta$  (the active portion in the thermal exchange with the coolant) and  $\tilde{q}_0''$  (the constrained parameter to have the same  $\tilde{q}_{w,max}''$  of the real element).

**Figure 3.11:** *Comparison among the 3D-MB and the equivalent thermal structure.*

Concerning the input function reported in equation (3.8), which is a fundamental element of the second step in figure 3.6, it consists of a set of governing equations, in which no thermal volumetric source has been considered, and all properties of materials have been defined as constant.

The following BCs have been adopted:

- Dirichlet and Neumann BCs among material interfaces;
- Neumann BC at the outer surface of the W;
- Robin BC at the inner surface of the CuCrZr layer in contact with the coolant.

Concerning the Neumann BC, the heat flux applied to the equivalent geometry has been corrected to preserve the same  $q_{w,max}''$  experienced by the 3D geometry according to equation (3.9) and connected to  $\theta$  as reported in equation (3.10).

Based on power conservation, the ratio between the heat flux at the outer surface of the optimised geometry and that at the coolant side is proportional to the ratio of the inner and outer radii. Since the heat flux on the coolant side has been assumed equal to that of the real geometry, this value has been corrected by introducing the  $f_p$  defined in equation (3.6). In this way, the heat flux applied to the outer surface of the optimised geometry can be expressed as reported in equation (3.9).

Similarly, the angle in equation (3.10) has been calculated so that the linear thermal power in the width ( $w$ ) of the 3D MB is the same as that of the arc of a circle characterised by  $r_3$  and amplitude  $\theta$ .

Furthermore, the HTC, in the equation (3.8) reported as  $h$ , has been assumed constant throughout the optimisation and equal to the average value of 136 kW/(m<sup>2</sup> K) in accordance with the results obtained with [56]. This choice has been made because implementing a temperature-dependent  $h$  would have significantly increased the complexity of the model and, consequently, the computational cost of the optimisation process in terms of machine time.

$$\left\{ \begin{array}{l} \rho_{CuCrZr} c_{CuCrZr} \frac{\partial T_{CuCrZr}}{\partial t} = \frac{\lambda_{CuCrZr}}{r} \frac{\partial}{\partial r} \left( r \frac{\partial T_{CuCrZr}}{\partial r} \right) \\ \rho_{Cu} c_{Cu} \frac{\partial T_{Cu}}{\partial t} = \frac{\lambda_{Cu}}{r} \frac{\partial}{\partial r} \left( r \frac{\partial T_{Cu}}{\partial r} \right) \\ \rho_W c_W \frac{\partial T_W}{\partial t} = \frac{\lambda_W}{r} \frac{\partial}{\partial r} \left( r \frac{\partial T_W}{\partial r} \right) \\ \lambda_{CuCrZr} \frac{\partial T_{CuCrZr}}{\partial r} \Big|_{r_0} = h (T_{CuCrZr}(r_0) - T_{water}) \\ \lambda_W \frac{\partial T_W}{\partial r} \Big|_{r_3} = \tilde{q}_0'' \\ \lambda_{CuCrZr} \frac{\partial T_{CuCrZr}}{\partial r} \Big|_{r_1} = \lambda_{Cu} \frac{\partial T_{Cu}}{\partial r} \Big|_{r_1} \\ T_{CuCrZr}(r_1) = T_{Cu}(r_1) \\ \lambda_{Cu} \frac{\partial T_{Cu}}{\partial r} \Big|_{r_2} = \lambda_W \frac{\partial T_W}{\partial r} \Big|_{r_2} \\ T_{Cu}(r_2) = T_W(r_2) \end{array} \right. \quad (3.8)$$

$$\tilde{q}_0'' = q_0'' f_p \frac{r_0}{r_3} \quad (3.9)$$

$$\theta = \frac{\tilde{q}_0'' w}{\tilde{q}_0'' r_3} \quad (3.10)$$

Since the analytical solution to the problem proposed in equation (3.8) can not be easily obtained, the temperature evolution has been calculated by employing the FDM. In particular, a second-order implicit method has been adopted and implemented in a MATLAB algorithm according to equation (3.11). The

temperature at node  $i$  at instant  $j$  and adjacent nodes at the same instant are determined from the temperature at node  $i$  at the previous instant  $j - 1$ .

$$-T_{i-1}^j \left[ \frac{\lambda \Delta t}{\rho c} \left( \frac{1}{\Delta r^2} + \frac{1}{2r_{i-1}\Delta r} \right) \right] + T_i^j \left[ \frac{2\lambda \Delta t}{\rho c \Delta r^2} + 1 \right] - T_{i+1}^j \left[ \frac{\lambda \Delta t}{\rho c} \left( \frac{1}{\Delta r^2} - \frac{1}{2r_{i+1}\Delta r} \right) \right] = T_i^{j-1} \quad (3.11)$$

To ensure the correctness of the problem solved by the developed FDM MATLAB model, a comparison has been carried out with the analytical steady-state solution of a multi-region cylindrical geometry without volumetric thermal generation, and applying a heat flux of 10 MW/m<sup>2</sup>. Furthermore, several tests have been performed by varying the number of radial nodes used to discretise the domain. This sensitivity analysis has been necessary because an excessively coarse discretisation leads to significant deviations from the analytical steady-state profile, whereas a sufficiently fine mesh ensures convergence and accuracy of the solution.

The comparison has been conducted using dummy values for tungsten thickness and thermodynamic and transport properties according to the initial value reported in table 3.4.

The figure 3.12 shows the consistency of the FDM model compared to the analytical solution; the obtained FDM trend is associated with an amount of 325 nodes across the fictitious cylindrical geometry, and the vertical dotted lines mark the border of the material layers.

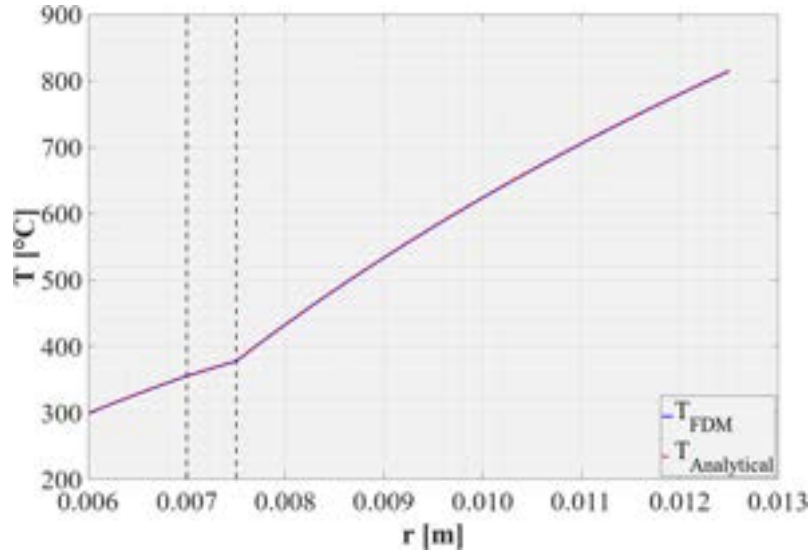


**Figure 3.12:** Check on the FDM algorithm in steady-state condition.

The selected nodes configuration is characterised at the inner and outer surface of the entire geometry from the following temperature relative error with respect to the analytical solution,  $\epsilon_w = 0.40\%$  and  $\epsilon_{CuCrZr} = 0.87\%$ .

The described radial discretisation has been chosen as a good compromise between the accuracy of results and the computing time to execute the FDM algorithm as the input function of the optimiser.

Therefore, through the detailed procedure described before and which is part of the second step of figure 3.6, it has been possible to obtain the optimised solution for the equivalent multilayer cylindrical thermal structure.

The GAMULTIOBJ has allowed obtaining 18 triads of optimum retrieved from the Pareto front (see figure 3.13), and by calculating the Euclidean norm on the errors to the objective functions of the optimiser, it has been possible to select the optimal solution, which is summarised in table 3.6.

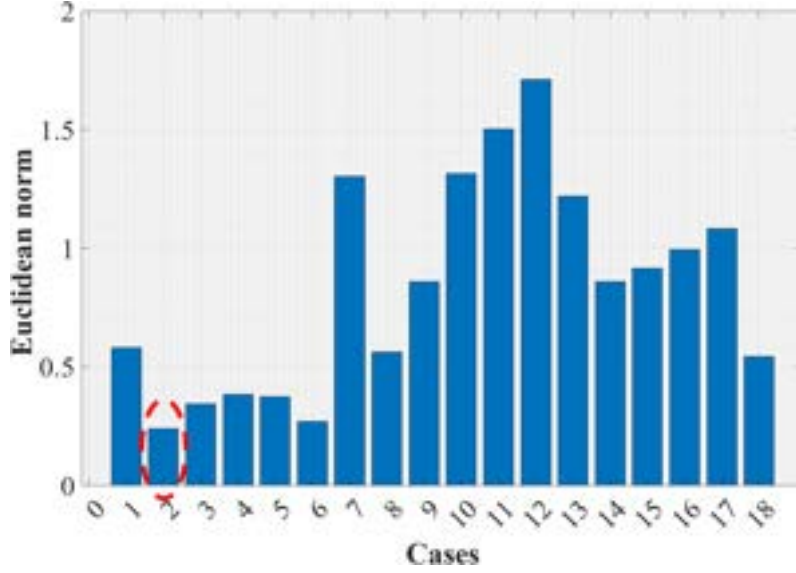


**Figure 3.13:** Set of optimum from the Pareto front.

**Table 3.6:** Selected optimal solution for each case.

|                      | Solution |
|----------------------|----------|
| <b>Triad</b>         | 2        |
| $t_w$ [m]            | 0.0145   |
| $c_p$ [J/(kg K)]     | 50.94    |
| $\lambda_w$ [W/(mK)] | 148.94   |

The optimal heat structure has been implemented inside the TRACE model of the simplified VT, and, in accordance with the third step of figure 3.6, it has been validated as shown in section 3.1.3.

### 3.1.2 Control system sub-model of simplified generic Vertical Target

The package of control system available in the TRACE code has been used to replicate the behaviour of the heat flux acting on the plasma facing surface of the simplified VT, considering the sweeping of a simplified Gaussian profile. In particular, the dedicated controller architecture has allowed to replicate the oscillating behaviour of the SP on the VT for all amplitude scenarios considered.

The schematic overview of the implemented control system logic is reported in figure 3.14. A generic control system architecture has been defined to simulate a transient scenario involving a loss of detachment event, starting from the normal operating conditions of the VT.

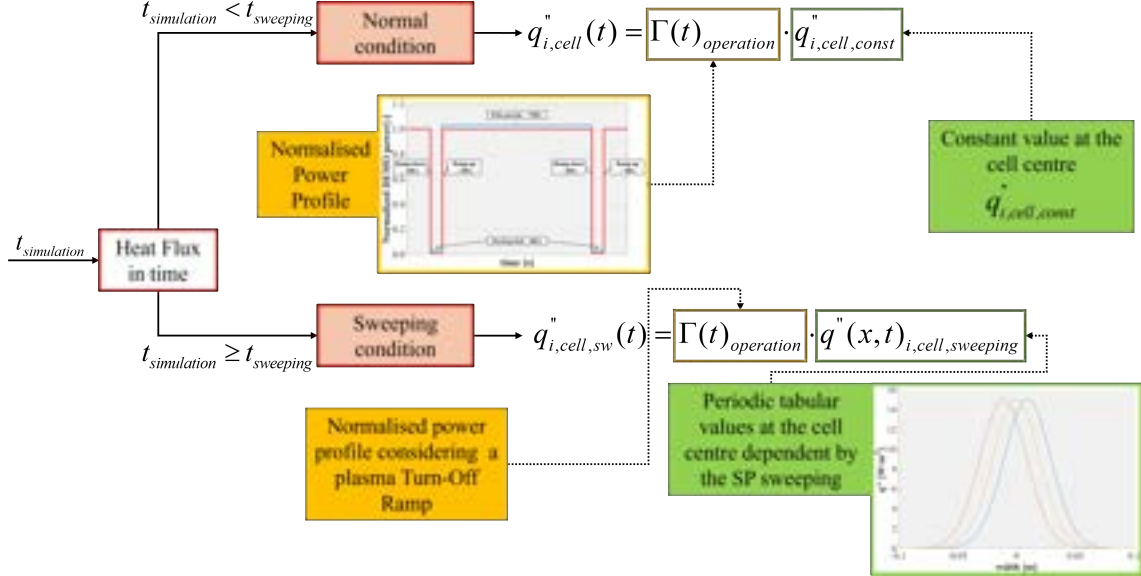


**Figure 3.14:** Schematic overview of the control system architecture.

Specifically, in the case of a simulation time shorter than the loss of detachment event, the outer plasma facing surface of all thermal cells of the equivalent heat structure (section 3.1.1) is characterised by an averaged heat flux value connected to the normal operating condition. This heat flux  $q''_{i,cell}(t)$  is computed as the product of a time-dependent function representing the normalised thermal profile during the duty cycle ( $\Gamma(t)_{operation}$ ) and a constant value specific to the average heat flux of each cell. The constant value ( $q''_{i,cell,const}$ ) is calculated so that the sum of the linear heat fluxes obtained by the product of average heat flux and the corresponding cell length is equal to the total linear heat flux of the VT.

If the simulation time is equal to or greater than the detachment loss event, the sweeping is simultaneous and the heat flux per cell is calculated as the product of two functions, the first time-dependent and the second time and space-dependent. In particular, in analogy with the normal operation, a time function ( $\Gamma(t)_{operation}$ ) common to all cells of the heat structure has been defined. The function has been provided for cases where a plasma shutdown ramp is defined. Indeed, in accordance with the case study, this function has been considered equal to one, which is the value that occurs under pulse conditions without taking into account any ramp associated with the plasma shutdown. Concerning the  $q''(x,t)_{sweep}$ , it is a time and space function specific to the single cell of the thermal structure determined by the application of the sweeping strategy, which has been calculated as reported in equation (3.3). The  $q''(x,t)_{sweep}$  have been provided to the TRACE model of a

periodic input table, whose values correspond to those calculated at specific time instants within a defined sweeping period. The table is constructed over a fixed time interval (e.g., 1 s), and during the simulation, it is repeated cyclically to cover the entire duration. For instance, in a 6-second simulation, the 1-second table is applied six times in sequence. This approach ensures that the time-dependent boundary condition reflects the periodic nature of the sweeping process throughout the entire simulation.

Therefore, the control system procedure defined for characterising the sweeping event has required the implementation of: 164 control system elements (including signal variables, control blocks, and trips), 13 dummy hydraulic pipes, 34 real variables, and 13 dummy tables.

The considerable number of components needed to simulate the behaviour of a simplified 20 cm VT highlights the necessity for a more immediate and less error-prone methodology.

A solution has been developed for the study of the DEMO MB (see section) and is based on the use of the ECI driver, as described in section 2.4.

### 3.1.3 Transient analyses and results of simplified generic Vertical Target

Once the simplified VT has been developed and implemented in TRACE, taking into account the application of the sweeping technique, the TH model has been studied. A parametric analysis varying the heat flux peak after choosing a specific sweeping condition has been carried out and compared with the results available in the reference study [30].

The simulations conducted involved the application of sweeping on a structure at thermal equilibrium at the initial time with a temperature of 200 °C. The loss of detachment event, as well as the application of the sweeping technique, has been set to occur instantaneously at the start of the simulation.

The simulation has been executed over a time span corresponding to six times the sweeping period, during which the relevant parameters of the system have been assumed to remain stable.

In particular, the following three different scenarios have been investigated as reported in table 3.7. A single pair of fundamental sweeping parameters (amplitude and frequency) has been defined and kept constant (50 mm and 1 Hz, respectively), while the heat flux peak has been varied.

**Table 3.7:** *TRACE investigated scenarios.*

|                                            |    |    |    |
|--------------------------------------------|----|----|----|
| <b>Amplitude [mm]</b>                      | 50 | 50 | 50 |
| <b>Frequency [Hz]</b>                      | 1  | 1  | 1  |
| <b><math>q''</math> [MW/m<sup>2</sup>]</b> | 15 | 20 | 30 |

The first two scenarios have been compared to the reference study [30] of which the table 3.8 shows the maximum and minimum temperature of W and Cu, instead, the third has been selected to observe the effects of the reaching of the CHF condition (as expected by the studies conducted in [29]).

**Table 3.8:** *Main output parameter of the reference study.*

| Scenario                     | 1       | 2        | 3 |
|------------------------------|---------|----------|---|
| $T_{W,max}/T_{W,min}$ [°C]   | 993/698 | 1281/896 | - |
| $T_{Cu,max}/T_{Cu,min}$ [°C] | 367/328 | 398/355  | - |

Furthermore, in order to compare the time profiles of the relevant parameters between the equivalent TRACE model and the real MB, a corresponding set of simulation has been carried out using the thermal model developed in ANSYS-CFX (section 3.1.1.2), which has proven to be highly faithful to the reference case study [30].

To ensure consistency with the optimised equivalent thermal structure, the ANSYS-CFX simulation has been performed assuming a temperature-independent convective heat transfer coefficient and an initial domain temperature of 200 °C.

Additionally, two distinct thermal structure concepts have been implemented in TRACE. The first one is directly associated with the hydraulic component simulating the VT, and is referred to as the equivalent TRACE thermal structure. The second concept, hereafter referred to as equivalent dummy TRACE thermal structure, is characterised by a constant convective heat transfer coefficient. This latter approach has demonstrated the advantage of the TH codes in identifying the flow regime without requiring any additional dedicated modelling strategies. The first equivalent thermal structure has enabled the observation of significant variations in the convective coefficient, particularly under CHF conditions.

The same scenarios have also been simulated using the developed FDM model. Its results, included in figures 3.15 to 3.17 for completeness, show a thermal response closely aligned with that of the TRACE dummy equivalent heat structure. Minor deviations are observed and are mainly attributable to the temperature-dependent material properties in TRACE, which are instead considered constant in the FDM model. Given the strong agreement between the FDM model and the TRACE dummy equivalent heat structure, the FDM results are not discussed in detail.

All results presented below refer to a single thermal cell, selected appropriately for comparison with the reference study [30].

Starting from the first scenario and in particular the figure 3.15a, the complex architecture of control system manages to replicate the swept thermal load used in ANSYS-CFX simulation quite well.

Concerning the figure 3.15b, a slight difference between curves can be detected due to the equivalent heat structure being optimised to capture the maximum

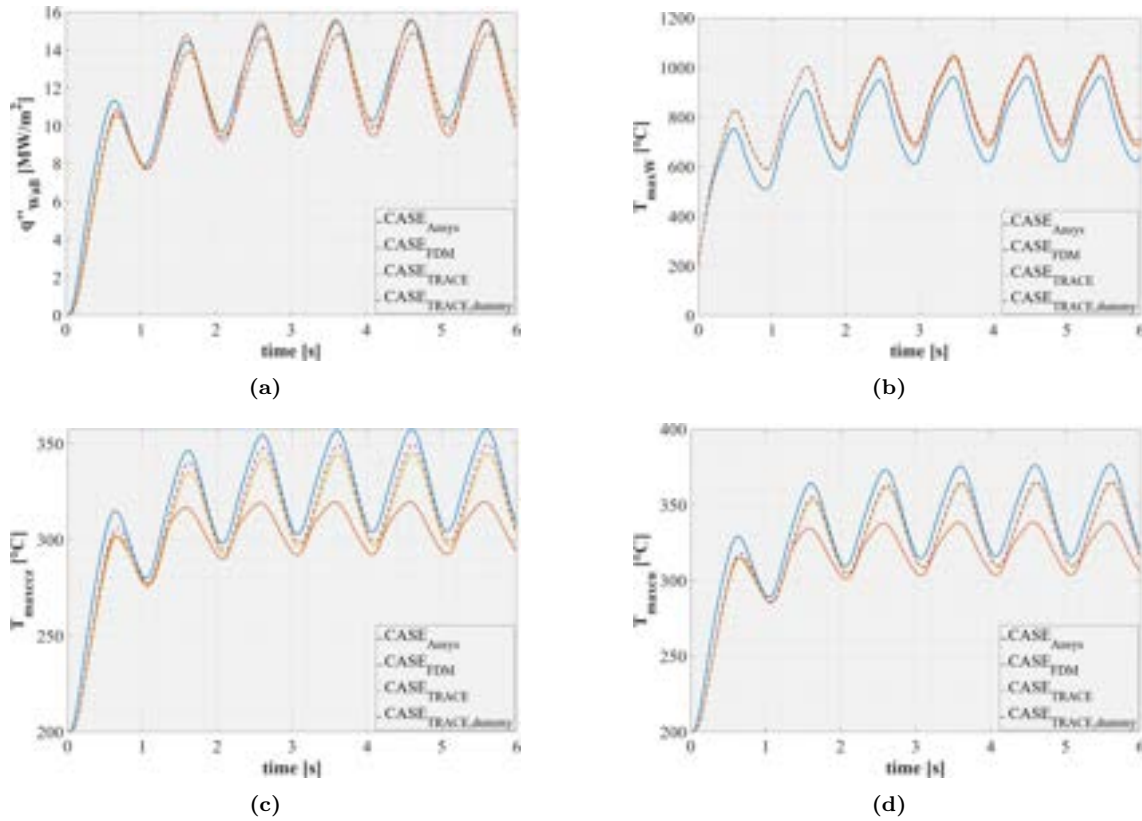


**Figure 3.15:** *Scenario 1 – Profiles of wall heat flux (a) and maximum temperatures for tungsten (b), CuCrZr (c), and Cu (d).*

W temperature. Quite different behaviour is observed for the CuCrZr and Cu temperatures, attributable to the approximation of the equivalent thermal structure, as can be seen from the comparison between ANSYS and TRACE dummy heat structure.

Furthermore, the table 3.9 shows the percentage errors on the characteristic parameters of the three simulations conducted against the output (table 3.8) of the reference study. Of particular interest are the errors of the equivalent heat structure linked to the hydraulic component (TRACE case), which are well below 10% compared to the reference study.

**Table 3.9:** *Scenario 1: percentage errors compared to the case study.*

| $\epsilon_{\text{MAX}}/\epsilon_{\text{MIN}}$ | TRACE     | TRACE <sub>dummy</sub> | ANSYS       |
|-----------------------------------------------|-----------|------------------------|-------------|
| $T_W$                                         | 5.0/-2.0% | 6.2/0.7%               | -2.7/-11.2% |
| $T_{\text{Cu}}$                               | 3.2/7.4%  | -0.4/-5.4%             | 2.6/-3.5%   |

Similar remarks can be made concerning the second scenario, whose relevant parameters are shown in figure 3.16. table 3.10 reports the percentage errors against the reference study, which differ from the first scenario and are slightly higher because the equivalent structure has been optimised with more influential weights for more demanding scenarios of 30 MW/m<sup>2</sup>.

It is worth noting that, in both scenarios (tables 3.9 and 3.10), the largest errors

are observed in the minimum temperature values, except for W in the first scenario. In contrast, the errors in the maximum temperature predictions are relatively modest.

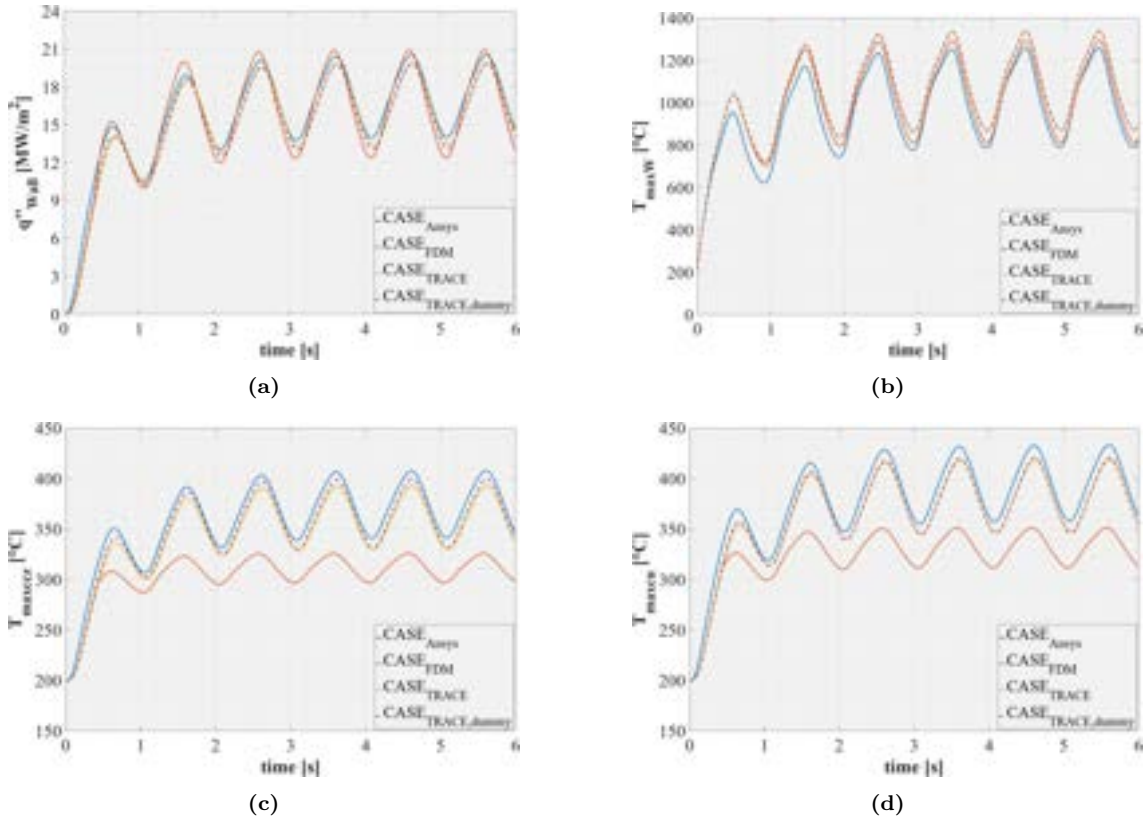


**Figure 3.16:** Scenario 2 – Profiles of wall heat flux (a) and maximum temperatures for tungsten (b), CuCrZr (c), and Cu (d).

**Table 3.10:** Scenario 2: percentage errors compared to the case study.

| $\epsilon_{\text{MAX}}/\epsilon_{\text{MIN}}$ | TRACE       | TRACE <sub>dummy</sub> | ANSYS       |
|-----------------------------------------------|-------------|------------------------|-------------|
| $T_W$                                         | 1.0/-9.2%   | 4.6/-2.8%              | -1.6/-11.8% |
| $T_{\text{Cu}}$                               | -1.0/-12.2% | -2.3/-18.0%            | 22.1/-0.8%  |

Regarding the third and final scenario (see figure 3.17), the results obtained from the TRACE model appear in line with what has already been demonstrated in [29]. In particular, in the TRACE equivalent thermal structure connected to the hydraulic pipe, it is possible to point out the reaching of the CHF condition.

Indeed, starting from a swept thermal peak of 30 MW/m<sup>2</sup> (figure 3.17a), the dummy and FDM heat structures, as well as the ANSYS-CFX real MB, show a substantial difference compared to TRACE. This clear discrepancy is due to the modelling assumption, where a constant convective coefficient has been set up not allowing the simulation of the two-phase behaviour. As a result, the corresponding thermal profiles do not accurately reflect the physical phenomena involved. Therefore, the subsequent analysis focuses exclusively on the TRACE case, which is capable of capturing the two-phase flow dynamics.

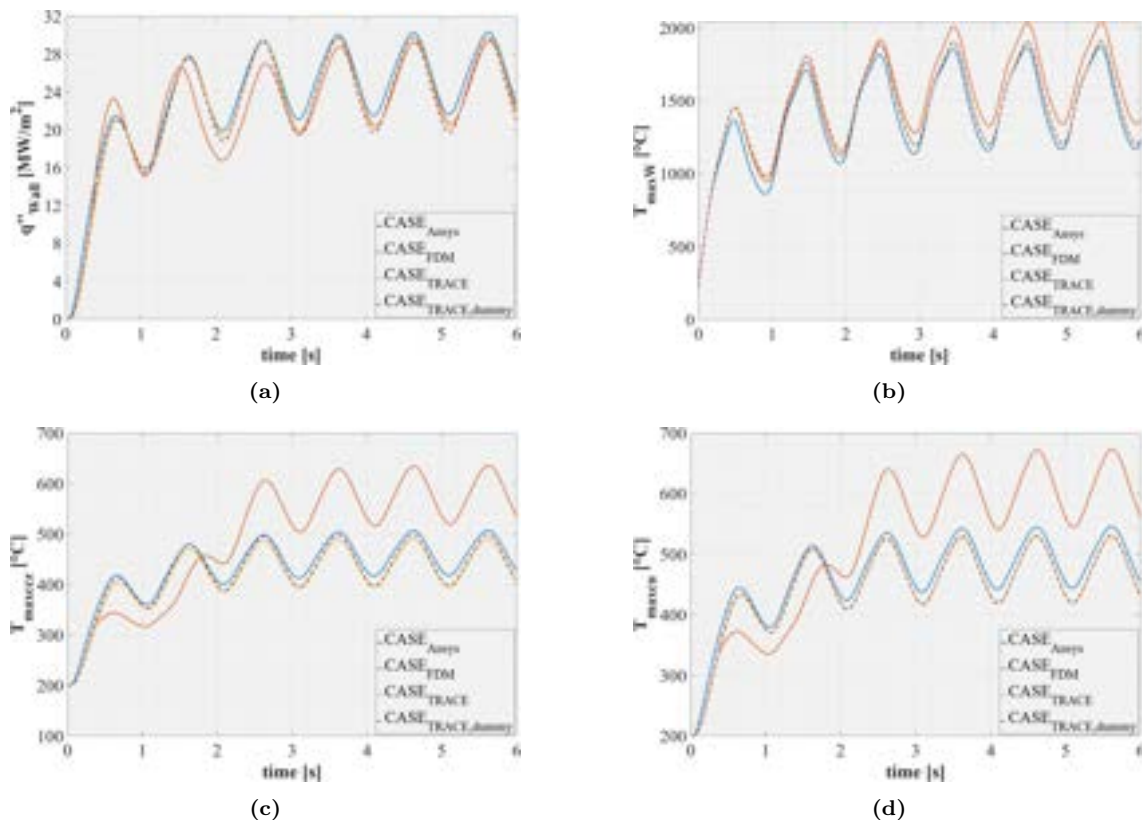


**Figure 3.17:** Scenario 3 – Profiles of wall heat flux (a) and maximum temperatures for tungsten (b), CuCrZr (c), and Cu (d).

The W temperature (figure 3.17b) increases gradually, remaining below the limits defined in section 1.3.4. Conversely, the temperatures of the CuCrZr and Cu layers (figures 3.17c and 3.17d) reflect the consequences of reaching the CHF condition. Both materials are subjected to elevated thermal cycles that exceed the permissible thresholds, as outlined in section 1.3.4.

A further relevant parameter that shows the advantages of the proper definition of a TRACE model, which couples the heat structure with the hydraulic component, is the assessment of the HTC during the studied transients.

In particular, the figure 3.18 shows the trend of the HTCs for the three distinct scenarios comparing the TRACE dummy heat structure characterised, as well as the ANSYS-CFX simulation, by a constant HTC.

The figure 3.18a shows the oscillating trend of the HTC of the first two scenarios, defined here as TRACE 1 and TRACE 2, in particular it is possible to observe an increase of the parameter with higher heat flux peak given the onset of a continuous two-phase flow in the case of the second scenario. In both scenarios, the sweeping technique allows for a positive increase in the coefficient, thus promoting the thermal exchange with the coolant. Concerning the figure 3.18b, it is possible to observe the reaching of the CHF condition, which causes a drastic lowering of the HTC. Even if subsequently in post-CHF conditions, the HTC remains at values in the order of 75 kW/(m<sup>2</sup>K).

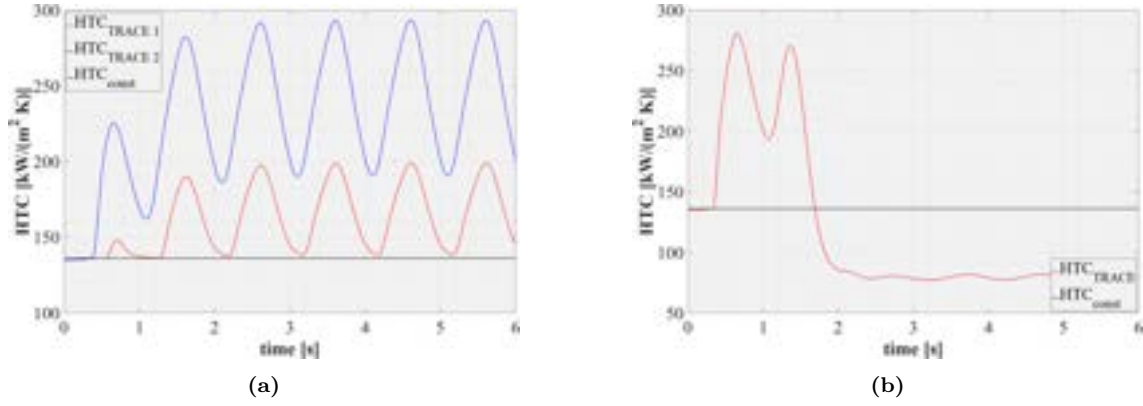


**Figure 3.18:** Behaviour of the HTC in Scenario 1 and 2 (a), and Scenario 3 (b).

Furthermore, the figure 3.19 shows the flow regimes calculated by the TRACE code during the sweeping application for all studied scenarios. In particular, the first scenario never experiences a two-phase flow, oscillating between the liquid single phase and the transition boiling, unlike scenario two, which remains during the transient in pre-CHF conditions. Instead, the third scenario, as already observed in figure 3.18b, reaches the post-CHF and the flow regime oscillates after a certain time between the inverted annular flow film boiling and the nucleate boiling.

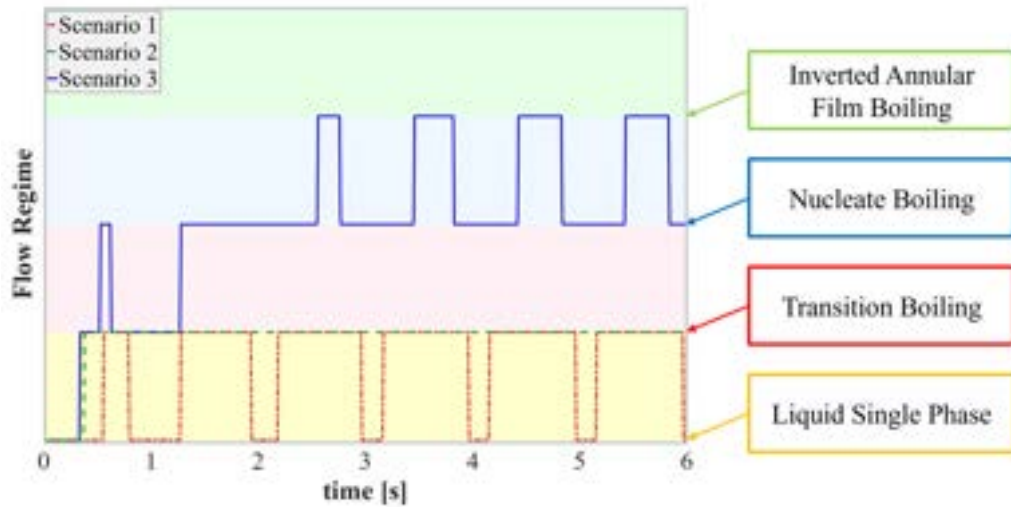


**Figure 3.19:** Evolution of flow regimes in all scenarios.

Therefore, the obtained results in line with [30, 29] have demonstrated the validity of the methodology developed to study the TH behaviour of the simplified VT by the use of TRACE. In particular, although the adoption of the simplified Gaussian profile to replicate the attached condition on the DIV, the percentage relative error of the relevant parameters varies slightly from [30]. Additionally, the use of the SYS Code allows to foresee the two-phase flow regime and the reaching of CHF and post-CHF conditions which have been obtained in [29].

The developed workflow has been adopted to study the same sweeping scenarios considering a simplified VT composed by DEMO-MB, taking into account the TH conditions of the baseline.

Finally, for the sake of completeness, the table 3.11 shows the simulation calculation times, which are to be understood as cumulative machine times, taking into account the number of processors used. This parameter is important not so much for the comparison that can be made between the two codes adopted, as they are of a different nature with a clear advantage for the SYS Code, which adopts an extremely simplified approach compared to ANSYS, but to show any improvements in calculation time with subsequent refinements of the modelling procedure of the domain under study by the use of TRACE.

Interestingly, the TRACE simulation times decrease as the thermal load increases across the three scenarios. This unexpected behaviour is explained by the evolution of the flow regimes. The first scenario (figure 3.19) remains predominantly in the single-phase liquid regime, which is numerically demanding due to the need for fine resolution of thermal-physical properties. In contrast, the last scenario rapidly reaches the post-CHF condition, entering the inverted annular film boiling regime. In this regime, the solver benefits from simplified physical models (see section 2.3.2) and stabilised numerical behaviour, resulting in shorter CPU times despite the higher thermal loads.

**Table 3.11:** *Cumulative CPU for the simulations.*

|                       | Simulation Time [s] |       |       | N.Processor |
|-----------------------|---------------------|-------|-------|-------------|
| Scenario              | 1                   | 2     | 3     | -           |
| TRACE                 | 0.036               | 0.006 | 0.005 | 1           |
| ANSYS <sub>real</sub> | 573                 | 578   | 586   | 10          |

## 3.2 Model set-up and study of simplified DEMO Vertical Target

In analogy with the previous study section 3.1, a simplified generic VT of 20 cm, composed by a single PFUs assembly, has been reproduced considering the current DEMO-MB and the TH data of the DIV baseline (section 1.3.2).

The simplified VT is reproduced in TRACE as a pipe of 13 hydraulic cells, and the same modelling strategies reported in section 3.1 have been used.

As follows, the major difference with the model presented in the section 3.1 will be treated to avoid conceptual repetition, taking for granted the similarities with the previous model.

Specifically, two distinct optimised multilayer cylindrical heat structures have been developed to replicate the front side region of the PFU fundamental unit.

These equivalent thermal configurations have been derived through a multi-objective optimisation process, each guided by a different set of performance criteria. The first optimisation targeted both the energy content of the PFU unit and the peak temperatures reached by the materials. The second focused exclusively on minimising the maximum temperatures of the materials.

Accordingly, the overall selected objectives have been:

1. the energy content of PFU-unit at the end of the thermal simulation in which the steady-state condition has been reached;
2. the maximum W temperature at 2.5 s from the application of thermal load;
3. the maximum W temperature at 5 s from the application of thermal load;
4. the maximum W temperature in the steady-state condition;
5. the maximum Cu temperature in the steady-state condition.

The set of target values, referring to the current DEMO-PFU fundamental unit, has been defined considering the two previously step-increase thermal cases. The table 3.12 below shows the targets derived from the analyses.

**Table 3.12:** Target values derived from thermal simulations.

| $q''$ [MW/m <sup>2</sup> ] | 15      | 30      |
|----------------------------|---------|---------|
| $X_{target,1}$ [J]         | 1220.50 | 2936.42 |
| $X_{target,2}$ [°C]        | 1430.74 | 2829.00 |
| $X_{target,3}$ [°C]        | 1649.49 | 3339.42 |
| $X_{target,4}$ [°C]        | 1720.48 | 3580.13 |
| $X_{target,5}$ [°C]        | 462.72  | 804.79  |

Each target has been weighted according to the table 3.13, where there is also a division between the optimisation of overall objectives (Case 1) and that referred only to the selected maximum temperatures of materials (Case 2).

| (a)                        |    |    | (b)                        |    |    |
|----------------------------|----|----|----------------------------|----|----|
| $q''$ [MW/m <sup>2</sup> ] | 15 | 30 | $q''$ [MW/m <sup>2</sup> ] | 15 | 30 |
| $W_1$                      | 4  | 8  | $W_1$                      | 0  | 0  |
| $W_2$                      | 1  | 2  | $W_2$                      | 1  | 2  |
| $W_3$                      | 1  | 2  | $W_3$                      | 1  | 2  |
| $W_4$                      | 2  | 4  | $W_4$                      | 2  | 4  |
| $W_5$                      | 1  | 2  | $W_5$                      | 1  | 2  |

**Table 3.13:** Weights used in the optimisation procedure for Case 1 (a) and Case 2 (b).

Specifically, greater importance has been given to the objectives associated with the highest heat flux scenario (30 MW/m<sup>2</sup>). Moreover, within each heat flux case, the steady-state temperature of W has been assigned a higher weight compared to the other temperature targets in both optimisation cases. In Case 1, for each heat flux, the highest weight has been attributed to the energy content. In contrast, in

Case 2, where the optimisation has excluded the energy content of the PFU unit, the corresponding weight ( $W_1$ ) has been set to zero, as shown in table 3.13b.

The input function adopted in the optimisation procedure required a review of the material properties of the equivalent thermal structure, as shown in table 3.14.

**Table 3.14:** Assumed thermodynamic and transport properties for DEMO PFU fundamental unit.

| Layer  | $c_p$ [J/(kg K)] | $\lambda$ [W/(m K)] | $\rho$ [kg/m <sup>3</sup> ] |
|--------|------------------|---------------------|-----------------------------|
| CuCrZr | 393.16           | 317.05              | 8893                        |
| Cu     | 395.99           | 392.94              | 8940                        |

Taking into account the procedure in section 3.1.1.2 and the revisions as reported before, the GAMULTIOBJ has allowed obtaining 18 triads of optimum retrieved from the Pareto front, for both cases.

By calculating the Euclidean norm (figure 3.20) on the errors to the objective functions of the optimiser, it has been possible to select the two optimal cases.

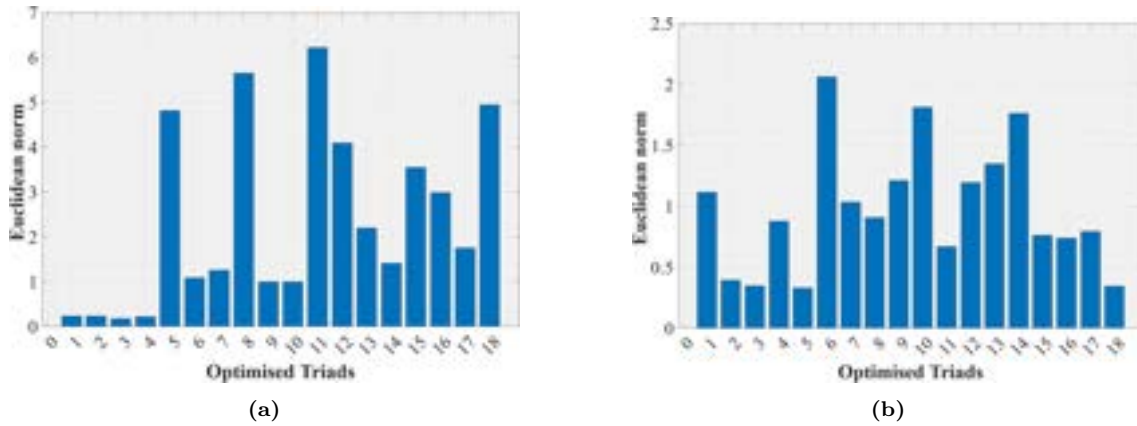


**Figure 3.20:** Euclidean norm of Pareto-optimal solutions for the equivalent thermal structure of the DEMO MB: Case 1 (a), Case 2 (b).

It could be observed that in Case 1, the calculated Euclidean norms are much larger than in Case 2, given the difficulty of optimising for all objectives caused by the necessary assumption of considering all material properties constant with temperature.

Therefore, the optimal triad has been selected for each case, as reported in table 3.15.

**Table 3.15:** Selected optimal solution for each case.

|                      | Case 1  | Case 2  |
|----------------------|---------|---------|
| Triad                | 3       | 5       |
| $t_w$ [m]            | 0.019   | 0.016   |
| $c_p$ [J/(kg K)]     | 30.421  | 29.380  |
| $\lambda_w$ [W/(mK)] | 119.030 | 115.273 |

Furthermore, with specific reference to the heat flux boundary condition applied to the thermal cells of the equivalent heat structure, a novel modelling strategy

has been implemented. This approach aims to minimise user-induced errors in configuring the complex architecture of the control system and to reduce development time. To this end, dedicated Python scripts have been employed to ensure the correct and consistent assignment of thermal flux to each cell, at every simulation time step, as a boundary condition on the outer surface of the thermal structure.

The procedure has been possible thanks to the ECI driver (section 2.4) and has completely replaced the logic of the control system reported in section 3.1.2.

The figure 3.21 depicts the dynamic interplay among the TRACE model, the ECI driver, and both internal and external Python scripts, which together form the computational framework for the transient simulation.

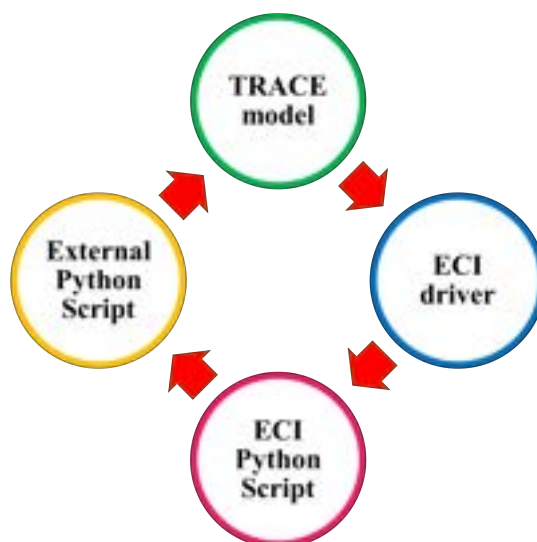


**Figure 3.21:** Schematic view of TRACE interacting with Python Scripts by the use of ECI.

The new approach has been primarily tested considering a transient event on which the sweeping technique is applied in the case of a simplified Gaussian heat flux profile. Nevertheless, it is also capable of accurately reproducing the Eich profile, which will be presented in the following section in reference to the complete DIV cassette equipped with both IVT and OVT to prevent redundancy.

Once the optimal solution among the set defined by the optimisation has been selected, both cases have been reproduced in a simplified TRACE model of the OVT portion analogous to figure 3.1 using properly the optimised parameter.

According to the baseline, the TH model has been simulated considering an inlet coolant velocity of 12 m/s, an outlet pressure of 5.0 MPa and an inlet/outlet coolant temperature of 130/136 °C.

Concerning the cells of the equivalent thermal structures of the 3D-MB, they are supposed to be subjected at the outer surface to a heat flux condition connected to the application of the sweeping procedure as in section 3.1.1, considering the same three distinct scenarios table 3.7.

### 3.2.1 Transient analyses of simplified DEMO Vertical Target

The scenarios have been investigated by the use of both ANSYS-CFX and TRACE simulations, and following thermal results, divided for the two optimised cases, are based on the comparison between:

- the thermal analysis of the real PFU fundamental unit in ANSYS-CFX with a constant HTC;
- the thermal analysis of the equivalent cylindrical PFU fundamental unit in ANSYS-CFX with a constant HTC;
- the thermal-hydraulic analysis of equivalent cylindrical PFU single assembly in TRACE system code.

In contrast to the generic VT case discussed in section 3.1.3, a TRACE dummy heat structure has not been defined. Instead, the thermal simulation of both optimised heat structures has been carried out using ANSYS-CFX, where the same tungsten radius, specific heat capacity and conductivity of the TRACE model have been adopted.

Similarly to table 3.11, table 3.16 reports the simulation times, highlighting the differences introduced by the use of the ECI driver.

**Table 3.16:** *Cumulative machine times for the simulations.*

|                             | Simulation Time [s] |     |     | N.Processor |
|-----------------------------|---------------------|-----|-----|-------------|
| Scenario                    | 1                   | 2   | 3   | -           |
| <b>TRACE - Case 1</b>       | 248                 | 248 | 249 | 1           |
| <b>TRACE - Case 2</b>       | 247                 | 249 | 244 | 1           |
| <b>ANSYS<sub>real</sub></b> | 742                 | 751 | 761 | 10          |

It is evident an increase in terms of cumulative CPU times, although it remains in any case below the threshold of ANSYS-CFX simulation. The effective advantage lies, if anything, in the simplification of the modelling procedure, which does not require any detailed modification of the control system architecture, but rather allows for the immediate execution of the code through the appropriate adjustment of the peak of the simplified Gaussian trend. This is possible through existing Python scripts capable of reprocessing the fluxes accordingly, distributing them, consistent with what has been discussed so far, across the entire domain. An additional benefit is the limited intervention required from the user and the possibility of employing alternative profiles, such as Eich, while freely refining the axial discretisation of the thermal structure to reproduce the thermal load profile as accurately as possible.

Contrary to the previous results section 3.1.3, it can be observed that the simulation time for TRACE across all optimised geometries and scenarios is quite similar. Furthermore, the ANSYS-CFX simulations are characterised by longer

computation times, possibly associated with geometric differences and therefore with the number of mesh elements.

### 3.2.1.1 Results for Case 1 of the Optimised Monoblock in the DEMO Vertical Target Configuration

Starting from Case 1 with the optimal solution 3, figure 3.22 shows the relevant thermal parameter of the optimised equivalent PFU fundamental unit considering a peak heat flux of  $15 \text{ MW/m}^2$ . In all graphs, the dotted lines of TRACE simulation and the green one are almost similar since based on the same thermal cylindrical fictitious PFU fundamental unit. The trends suggest that optimising for all targets is almost impossible in order to reproduce the red line of the real geometry. Indeed, while the interaction of the coolant figures 3.22b and 3.22c are reproduced at the final period of the simulation, on the other hand, the energy content and W temperature vary significantly from the target.

In the case of a  $20 \text{ MW/m}^2$  peak (figure 3.23), the optimised geometry of Case 1 has the same behaviour as the previous scenarios, and the same observations previously discussed have applied here as well.

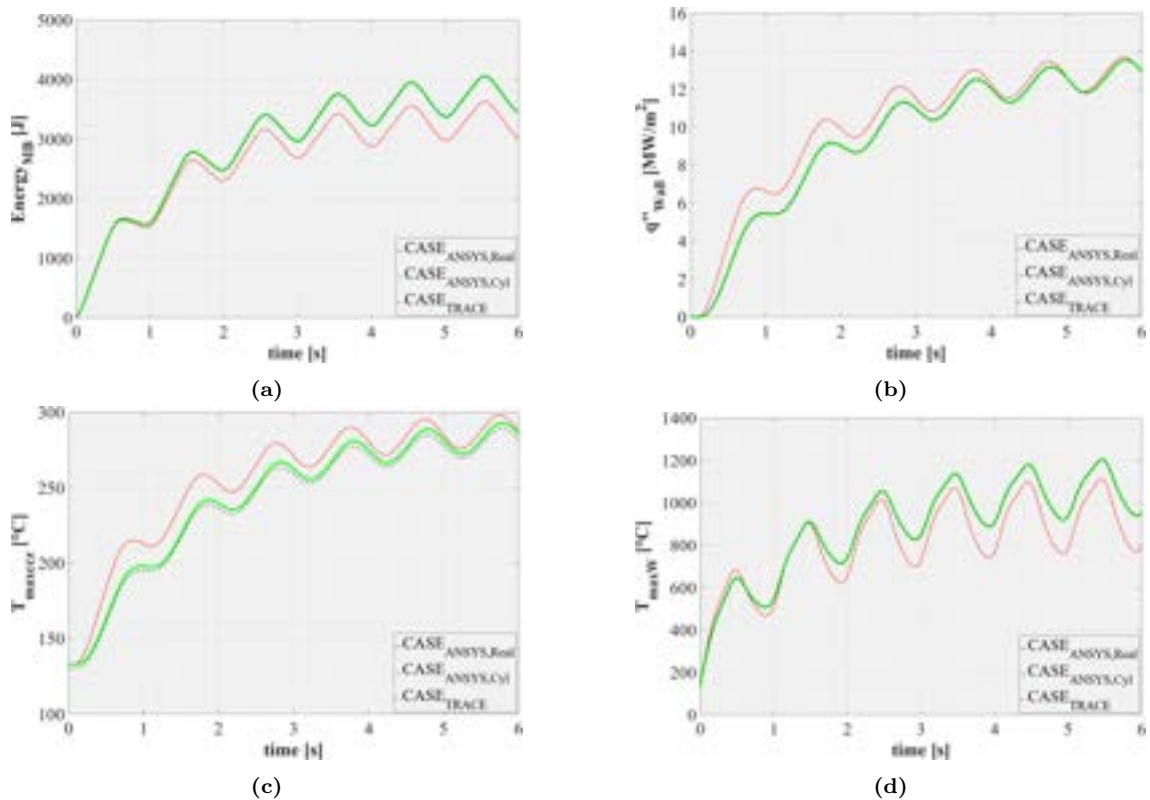


**Figure 3.22:** Thermal behaviour Case 1 scenario 1: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d).

Concerning the last scenario of Case 1, figure 3.24 has shown a better behaviour of the TRACE heat structure in term of energy content and W temperature.

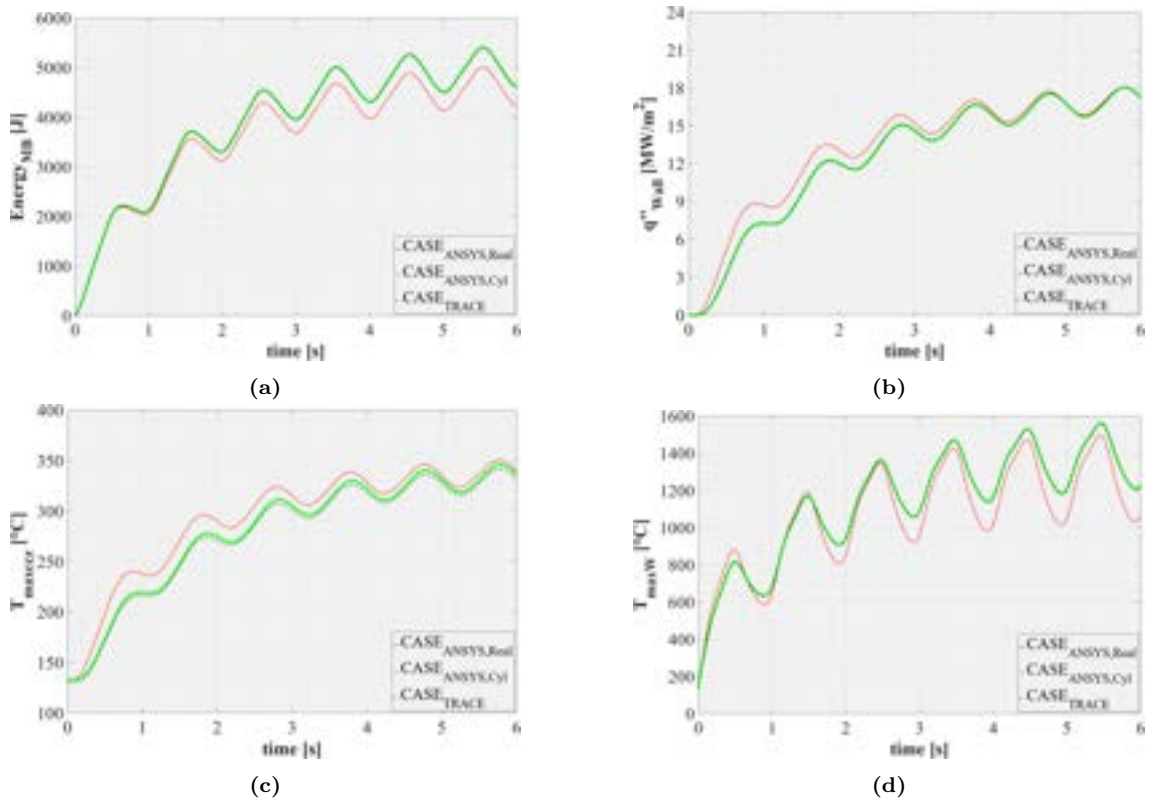


**Figure 3.23:** Thermal behaviour Case 1 scenario 2: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d).

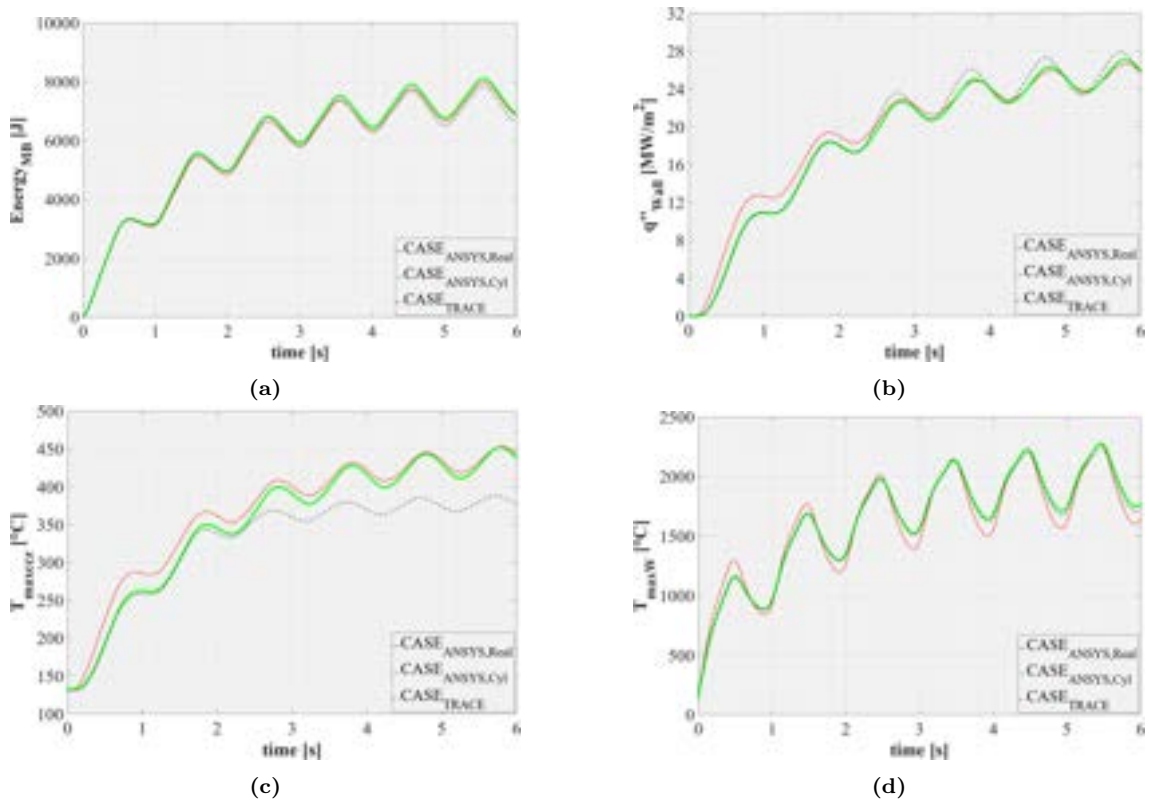


**Figure 3.24:** Thermal behaviour Case 1 scenario 3: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d).

This improvement has occurred compared to the scenario with a peak of  $15 \text{ MW/m}^2$ , since greater weight has been assigned to the targets under the most demanding thermal load conditions during the optimisation process.

On the other hand, increasing the heat flux peak leads to a greater deviation in wall heat flux and CuCrZr temperature. This occurs because, unlike ANSYS-CFX simulations, TRACE allows the calculation of a variable convective heat transfer coefficient, highlighting the positive effect of the coolant during transients.

Unlike the previous model described in section 3.1.3, due to the differing geometry of the MB and the boundary conditions of the TH, the CHF margins have not been exceeded.

### 3.2.1.2 Results for Case 2 of the Optimised Monoblock in the DEMO Vertical Target Configuration

Generally, it may observe an analogous behaviour to Case 1 for Case 2 in terms of the equivalent thermal structure analysed by the two methodologies, i.e thermal analyses in ANSYS-CFX and thermal-hydraulic assessment by the use of TRACE. Specifically, the profiles are sufficiently aligned when the influence of heat flux on convective coefficient variations is moderate, while a conspicuous deviation is observed when there are considerable convective coefficient variations from the value that has been imposed in ANSYS-CFX.

The complete set of results for Case 2 is presented in figures 3.25 to 3.27, where the thermal response of the optimised monoblock is analysed for increasing peak heat flux values.

Contrary to Case 1, the energy content is underestimated while in figures 3.25b and 3.25c the red curves are replicated more faithfully at the start of the sweeping application. In the case of figure 3.25d, the W temperature is overestimated even if the oscillations are more similar to the red line. The reason that the W temperature maxima and minima are not immediately detected is associated with the fact that the optimiser foresees increased weights under heavier heat flux conditions for the equivalent thermal structure.

Indeed, considering the case of a  $20 \text{ MW/m}^2$  peak, as shown in figure 3.26d, the range of variation is limited, maintaining the oscillations similar to the real PFU fundamental unit. Concerning the remaining parameters (figures 3.26a to 3.26c), similar behaviours of the previous scenario can be observed.

Referring to the last scenario shown in figure 3.27, an improvement in temperature detection W can be observed compared to the scenario with a peak of  $15 \text{ MW/m}^2$ , as already observed in section 3.2.1.1.

Furthermore, the energy content is fully dissimilar to the real PFU fundamental unit as reported on figure 3.27a.

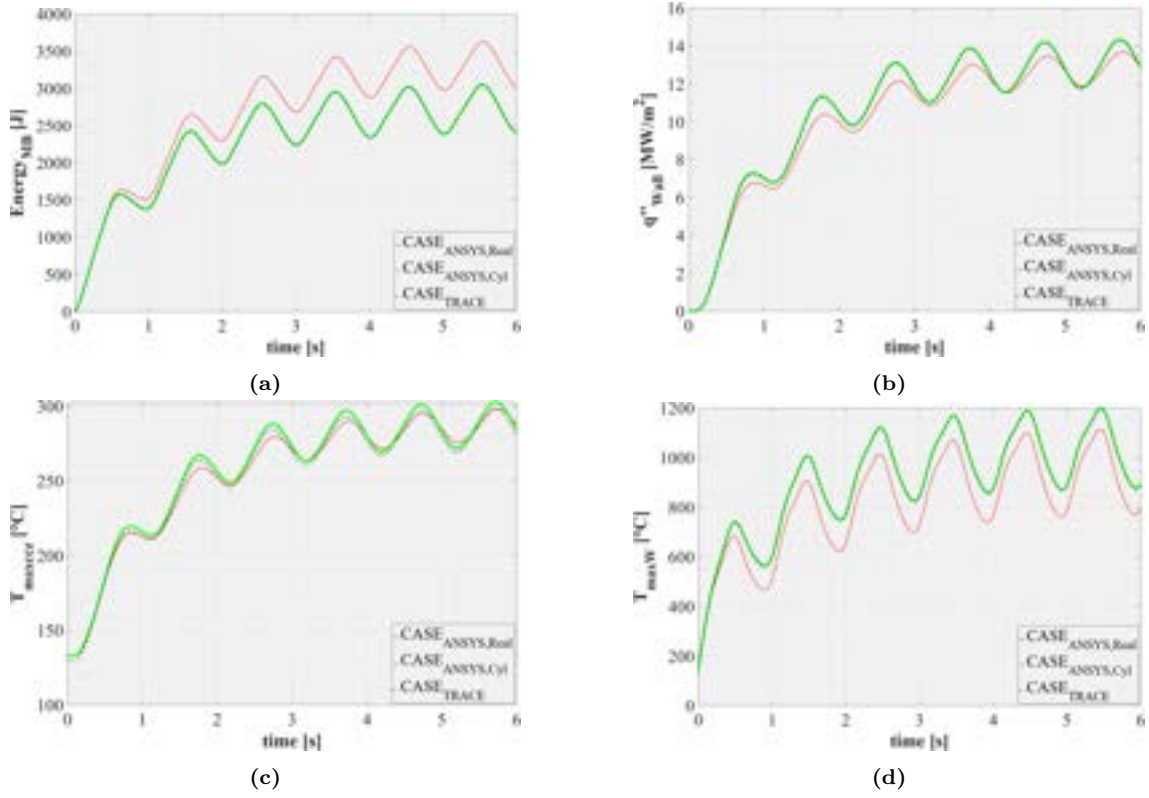


**Figure 3.25:** Thermal behaviour Case 2 scenario 1: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d).

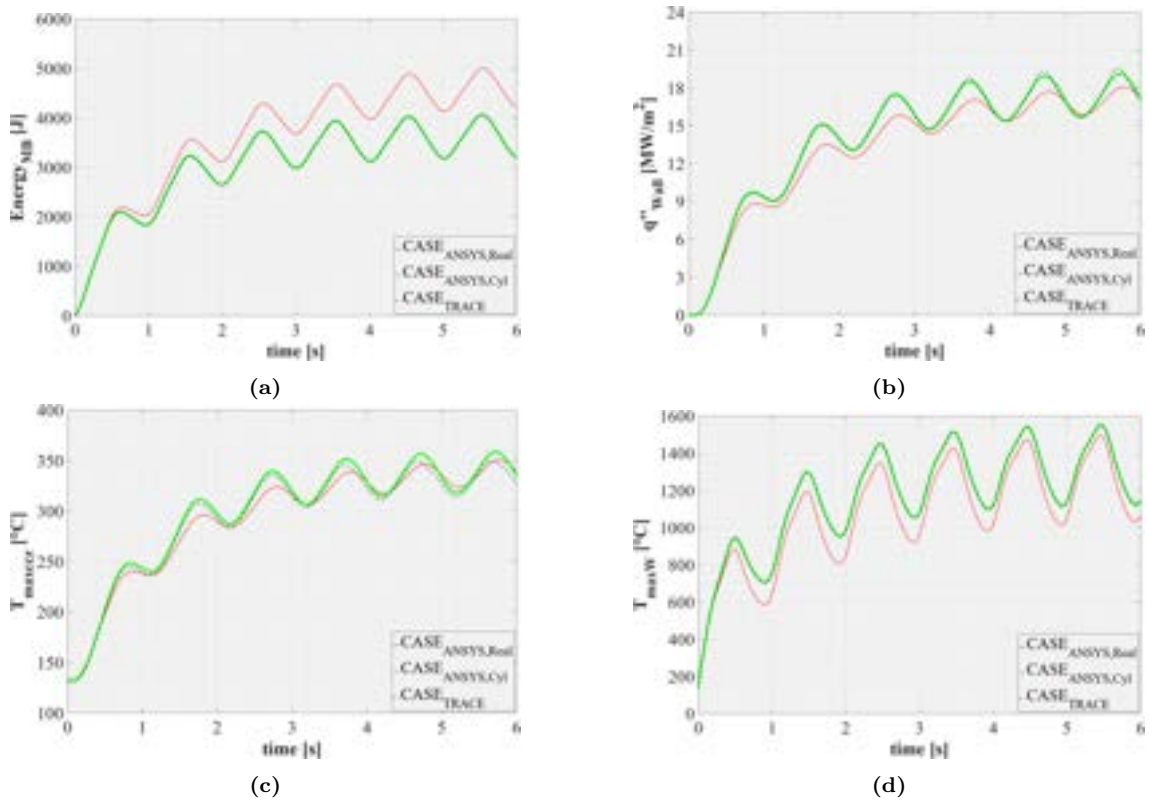


**Figure 3.26:** Thermal behaviour Case 2 scenario 2: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d).

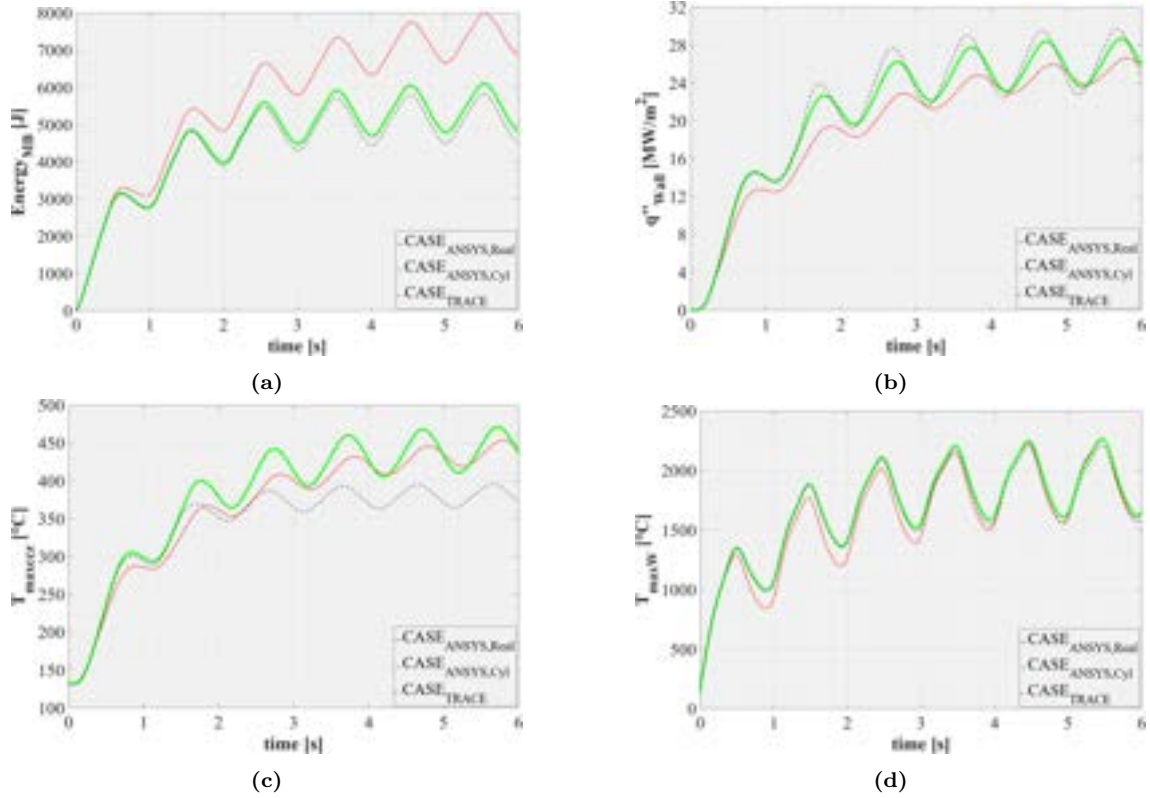


**Figure 3.27:** Thermal behaviour Case 2 scenario 3: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr maximum temperature (c), W maximum temperature (d).

### 3.2.2 Improvements on the DEMO Vertical Target

The analyses conducted for both optimised configurations reveal the inherent difficulty in satisfying all the optimisation objectives simultaneously. The choice between different geometries is often determined by the emphasis placed on specific results, making it difficult to achieve a balanced compromise.

Moreover, in both cases, the ability of the equivalent PFU fundamental unit to replicate the real thermal behaviour is strongly dependent on the peak heat flux associated with the studied transient. Indeed, the two equivalent thermal structures have been obtained considering greater weights to the targets associated with the scenario characterised by the highest thermal loads.

This variability of results is further compounded by the simplifications adopted in the optimisation procedure. Specifically, the material properties are assumed to be constant with temperature, and the equivalent cylindrical MB is defined using W material with fictitious thermodynamic and transport properties.

Taking into account these limits, a new procedure to define the heat structure has been developed. In particular, an analytical geometry has been calculated in order to capture the energy content of the PFU fundamental unit and a post-processing correction of the W maximum temperature has been performed using a regression model.

Referring to the new equivalent geometry derived by analytical calculations, the only parameter to be assessed has been the outer radius ( $r_3$ ) of the equivalent thermal structure (figure 3.28), while the properties of W materials are free to vary with temperature.

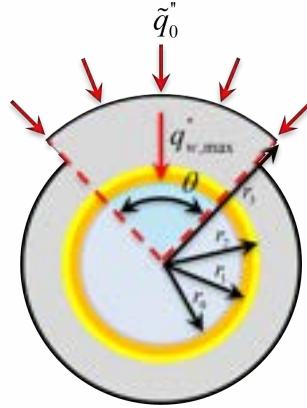


**Figure 3.28:** *Sketch of the analytical geometry.*

As a first step, it has been necessary to define by the analyses of the results of the real geometry in ANSYS-CFX the A surface which participates effectively in heat exchange figure 3.29.

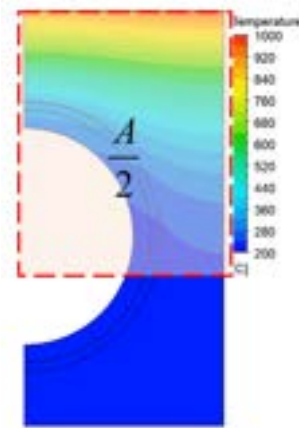


**Figure 3.29:** *View of the half surface participating in the heat exchange.*

The surface has been calculated as reported in equation (3.12) where  $f\%$  is the fraction of the total surface characterised by an evident thermal gradient,  $w$  and  $t_w$  are respectively the width and the thickness of the W-MB (see figure 3.7) and  $r_2$  the outer radius of Cu layer.

$$A = f\% \left[ w(t_w + r_2) - \pi \frac{r_2^2}{2} \right] \quad (3.12)$$

Consequently, the outer radius of W layer has been calculated by considering the set of equations reported in equation (3.13). These equations preserve, respectively:

the acting heat flux on the surface to ensure the same wall heat flux, the active surface area dedicated to thermal exchange, and the energy content of the PFU fundamental unit.

$$\begin{cases} f_p q''_{\text{real,MB}} = \tilde{q}''_{\text{cyl,MB}} \frac{r_3}{r_0} \\ A = \theta \frac{r_3^2 - r_2^2}{2} \\ \tilde{q}''_{\text{cyl,MB}} \theta r_3 = q''_{\text{real,MB}} w \end{cases} \quad (3.13)$$

Concerning the post-processing correction of the W maximum temperature, its trend has been predicted by the use of the regression learning MATLAB Toolbox [58]. The figure 3.30 shows the workflow for training regression models in the corresponding MATLAB environment.

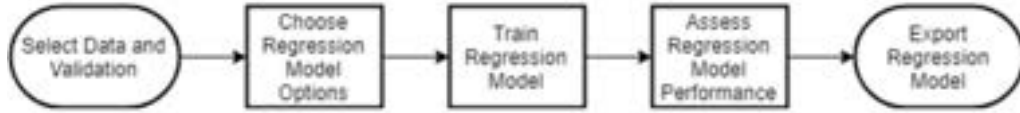


**Figure 3.30:** Flow chart for training the regression model [58].

As a starting point, the regression model requires data on which to train, and for this reason, the results already available from the ANSYS-CFX simulations conducted on the real PFU fundamental unit have been adopted. Specifically, the energy content of the PFU fundamental unit and the W temperature behaviour have been used as data in both step-increase and sweeping conditions.

Concerning the selection of the regression model, the linear one has been chosen for its computational efficiency and simplicity. Linear regression is lightweight and fast to train, even on large datasets, and has been found to be adequately suited to the characteristics of the available data.

Once the trained model has been obtained, it has been possible to define in post-processing the W maximum temperature based on the energy content retrieved from the TRACE simulations.

The figure 3.31 summarises the previously described steps, visually clarifying the connections between the ANSYS-CFX simulations and the post-processing temperature correction of TRACE model.

Once the proper outer radius has been calculated and the linear regression model has been trained, the same three thermal transient scenarios of the table 3.7 have been performed. The table 3.17 below shows the computing time for all simulation tools, including the study of the equivalent thermal structure by the use of ANSYS-CFX. The computing times of TRACE are analogous to table 3.16. The simulation times for the actual PFU fundamental unit in ANSYS-CFX are longer because the temperature dependence of the HTC has been implemented. The simulation times for the equivalent cylindrical geometry in ANSYS-CFX are

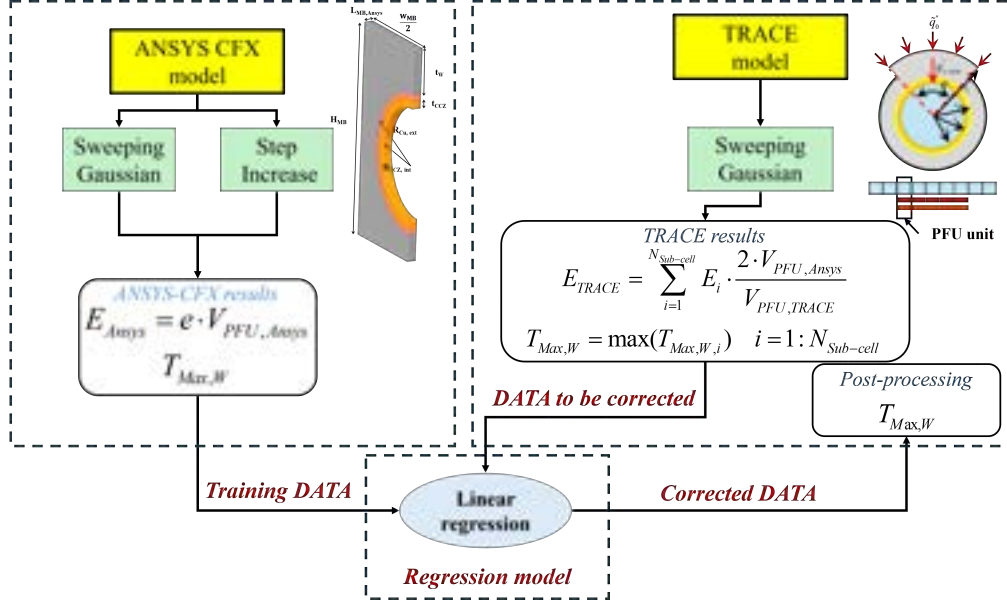


**Figure 3.31:** Procedure for correcting tungsten temperature by the use of the linear regression model.

shorter and show similar behaviour across scenarios, as the investigated domain (in terms of the number of elements and nodes) is smaller than in the real case.

**Table 3.17:** Cumulative machine times for the simulations.

|                       | Simulation Time [s] |      |      | N.Processor |
|-----------------------|---------------------|------|------|-------------|
| Scenario              | 1                   | 2    | 3    | -           |
| TRACE                 | 252                 | 253  | 280  | 1           |
| ANSYS <sub>cyl</sub>  | 559                 | 559  | 562  | 10          |
| ANSYS <sub>real</sub> | 1060                | 1289 | 1307 | 10          |

The relevant results are collected in figures 3.32 to 3.34. Both thermal models in ANSYS have been modelled considering a HTC which is a function of the coolant bulk temperature provided in table form and calculated using [56].

Starting from the first scenario figure 3.32, as expected, the behaviour of the analytical geometry by the use of both tools is perfectly in line, except for the maximum temperature of the CuCrZr layer. This can be attributed primarily to the different calculations proposed by the codes on the HTC. Indeed, while in both ANSYS-CFX the simulations have been conducted considering a temperature-dependent coefficient, calculated according to [56] where the Sieder-Tate correlation is adopted, in the case of TRACE, it is used the Gnielinski correlation [38] appropriately modified to replicate the behaviour of the Manglik-Bergles correlation [59]. Furthermore, the TRACE model is capable of replicating the entire PFU assembly, composed of 13 equivalent elements interacting with the coolant, by solving the temperature field in one dimension. In contrast,

ANSYS models only a single MB unit, capturing the thermal evolution in two dimensions (radial and poloidal) within the domain.

The trends shown in figures 3.32b and 3.32c for the TRACE model are similar to the actual fundamental unit MB. A similar observation can be made for the temperature W, which has been obtained through the use of the regression model in post-processing, thus allowing the black profile, obtained from the simulation with the SYS Code, to be shifted to the green one figure 3.32d.

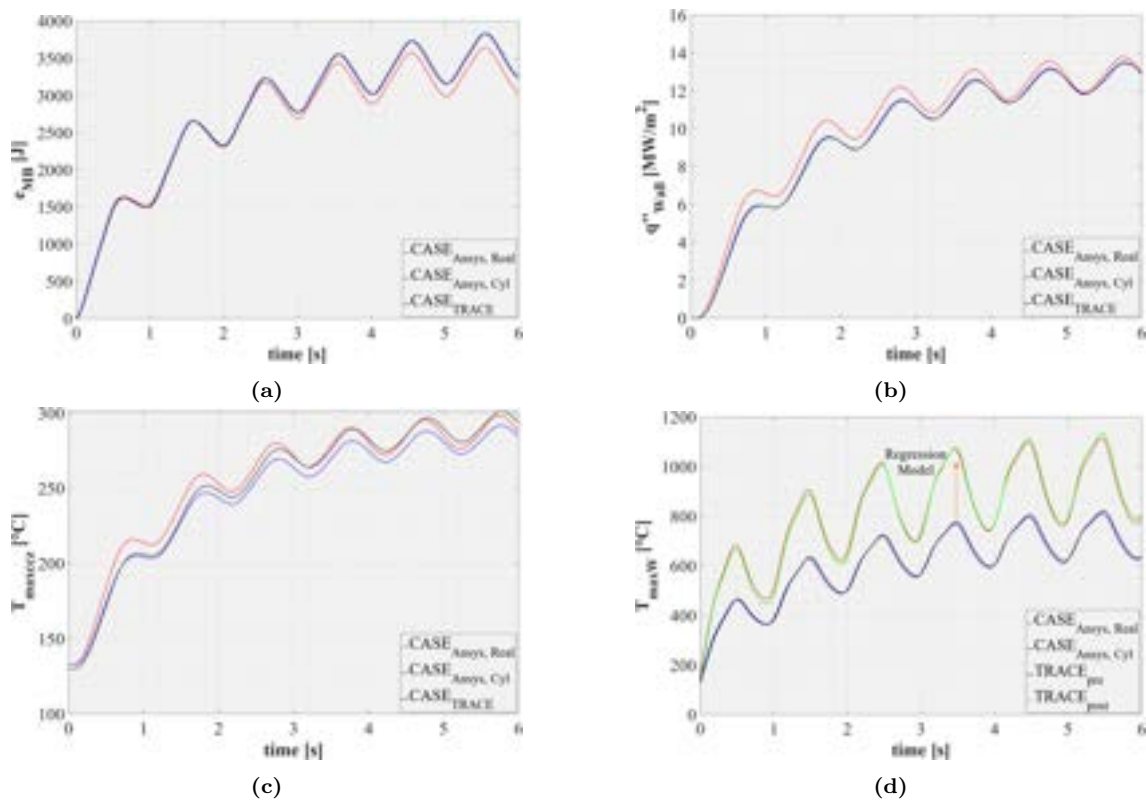


**Figure 3.32:** Thermal behaviour of the analytical geometry scenario 1: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

It is interesting to note how adopting temperature-dependent material properties has enabled the energy content to be replicated from the start of the transient, although slight differences remain in the steady state due to the inevitable simplification of the behaviour of the effective geometry figure 3.32a.

Similar observations can be made for the second and third scenario figures 3.33 and 3.34, where all of the plotted variables (figures 3.33a to 3.33c and 3.34a to 3.34c) of TRACE model increasingly capture the trend of the red curves. The linear regression model successfully adjusts the W temperature (figures 3.33d and 3.34d), demonstrating the robustness of the procedure and its applicability to various transient conditions.

Once again, the differences observed are related both to the distinct treatment of the HTC by each code, and to the different geometries modelled, as well as to

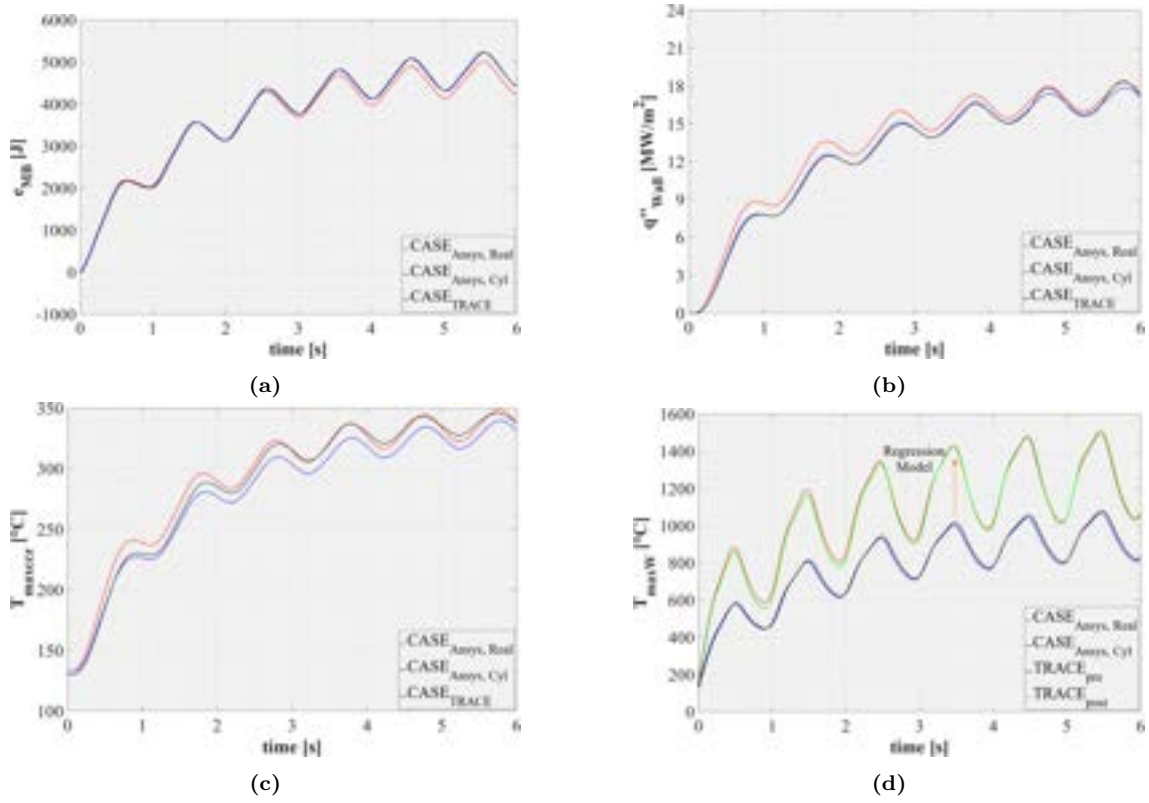


**Figure 3.33:** Thermal behaviour of the analytical geometry scenario 2: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

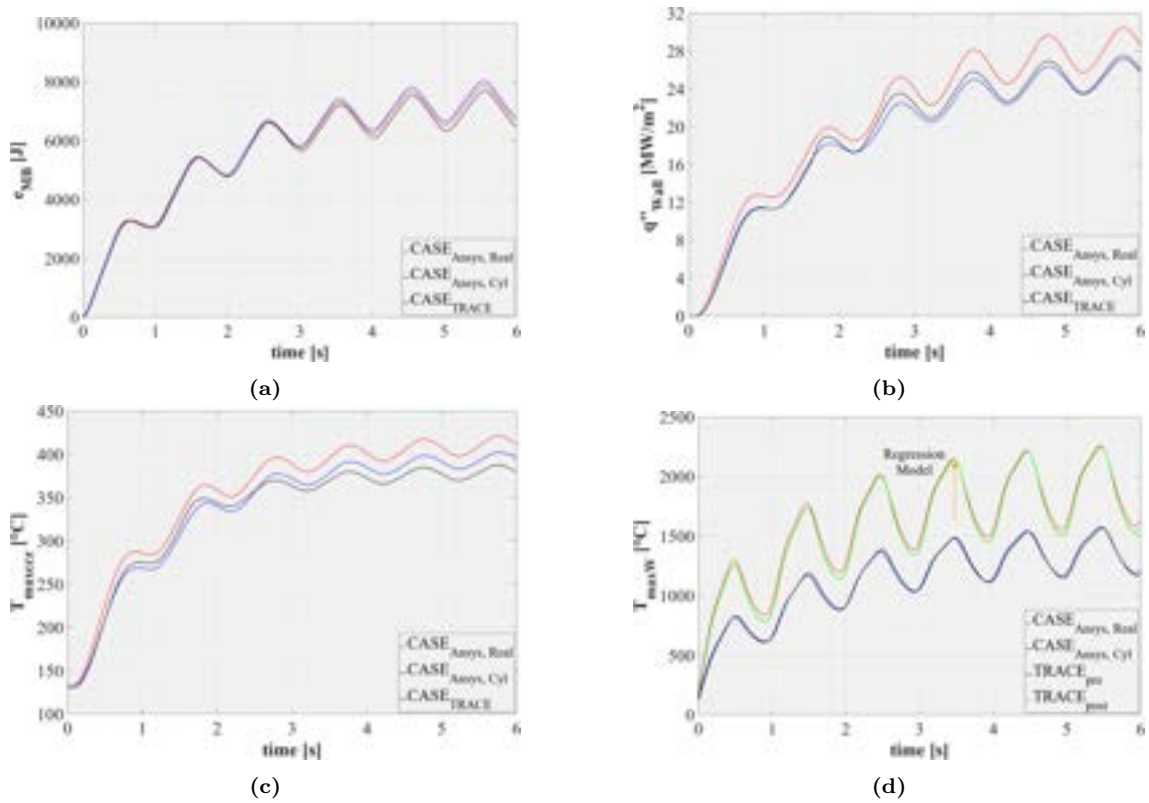


**Figure 3.34:** Thermal behaviour of the analytical geometry scenario 3: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

the distinct approaches adopted by the tools in solving the thermal field within the domain.

In any case, the percentage errors on the material temperatures from the TRACE simulations, compared to the actual ANSYS geometry, remain below 10%, as reported in the table 3.18 for all the investigated scenarios.

**Table 3.18:** *Percentage errors of TRACE simulations compared to the real PFU fundamental unit.*

| $\epsilon_{\text{MIN}}/\epsilon_{\text{MAX}}$ | Scenario 1 | Scenario 2  | Scenario 3   |
|-----------------------------------------------|------------|-------------|--------------|
| $T_{\text{W}}$                                | 9.75/2.89% | 3.89/2.33%  | -0.87/0.49%  |
| $T_{\text{Cu}}$                               | 1.93/0.46% | 1.00/-1.58% | -7.20/-8.55% |
| $T_{\text{CCZ}}$                              | 2.95/1.84% | 2.42/-0.52% | -6.51/-7.63% |

It is important to note that, in all the scenarios studied and using both the SYS Code and ANSYS-CFX tools, the DEMO PFU unit is not subject to post-CHF conditions.

Furthermore, when thermal loads on the VT are applied using a simplified Gaussian distribution to investigate the loss of detachment event in combination with sweeping, several requirements outlined in section 1.3.4 are not met.

Specifically, in the third scenario, the temperature of the CuCrZr oscillates above 350 °C. Additionally, in scenarios 2 and 3, the recrystallisation temperature for W is rapidly reached.

The results obtained and the outcomes derived demonstrate the validity of the developed methodology and confirm the suitability of the TRACE tool for studying this type of transient event.

Consequently, it becomes essential to evaluate the overall behaviour of a DIV cassette under a realistic loss of detachment event. The following section presents the study conducted using TRACE on a single cassette under the aforementioned conditions.

This last analytical equivalent PFU unit has been adopted in terms of radial and poloidal dimensions, adopting additionally the properly tuned Python scripts to reproduce the Eich heat flux profile on both VTs.

### 3.3 Model set-up and study of the DEMO Divertor Vertical Targets in sweeping condition

Once the PFU single unit modelling strategy has been chosen and the validity of the methodology has been demonstrated, the analytical geometry obtained in section 3.2.2 has been replaced on both VTs of the TRACE model, which reproduces the single PFCs in-vessel cooling circuit (section 4.1).

The figure 3.35 depicts the view of the circuits which replicates the flow path of figure 1.13. The pipe arrangement in figure 3.35 is only adopted to make it clear the modelled fundamental channels.

The overall domain has been studied considering an inlet/ outlet temperature of 130/136 °C as well as a pressure of 5.0 MPa with a mass flow rate of 98.63 kg/s.

Both VTs are characterised by an equivalent single pipe which mimics the correct number of PFU assemblies.

Furthermore, both hydraulic pipes related to VTs are discretised in order to approximately preserve the axial length of the PFU fundamental unit, incorporating the gaps between the MBs within the hydraulic cell of the pipe. The heated diameter has been defined to take into account the presence of the ST from a thermal point of view.

Concerning the overall hydraulic model, the concentrated and distributed hydraulic resistances have been modelled considering their possible functional dependence on the flow velocity field, similarly to what is described in [17].

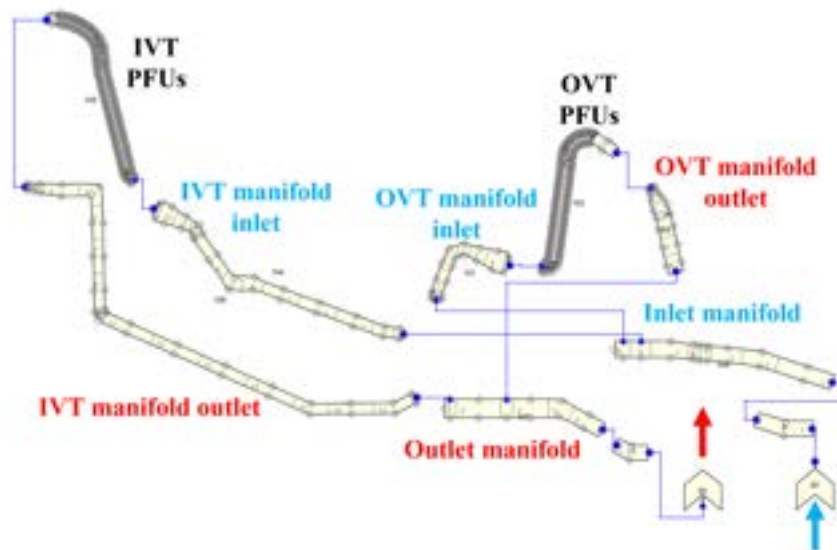


**Figure 3.35:** *SNAP view of the PFCs in-vessel cooling circuit.*

### 3.3.1 Thermal sub-model of the DEMO Divertor Vertical Targets in sweeping condition

To complete the modelling of the PFU assembly (based on section 3.2.2), since the only front side of the domain has been reproduced, being the effective section actively interested by the heat flux from the plasma, the back side of the component (see section 3.1.1) has been calculated.

The equivalent heat structure of the back side (figure 3.36), which in turn is a cylindrical multilayer geometry, has been obtained according to the equation set

reported in equation (3.14). Specifically, the angle  $\theta_{back}$  and the outer radius  $r_{3,back}$  of the W layer have been defined preserving the volume of the PFU fundamental unit.

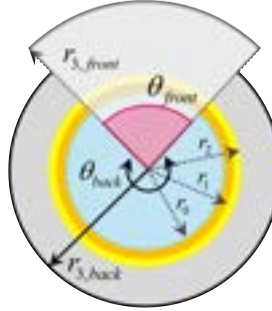


**Figure 3.36:** Sketch of the thermal structure of the back side of PFU.

$$\begin{cases} \theta_{back} = 2\pi - \theta_{front} \\ A_{MB,struct} = A_{front} + A_{back} \\ A_{back} = \theta_{back} \left( \frac{r_{3,back}^2 - r_0^2}{2} \right) \end{cases} \quad (3.14)$$

Therefore, an outer radius of 0.012 m and an angle of 3.78 rad have been calculated and implemented in the couple of the heat structures directly connected to the hydraulic model of both VTs as shown in figure 3.37 where each hydraulic cell in blue corresponds to the thermal cell in red.

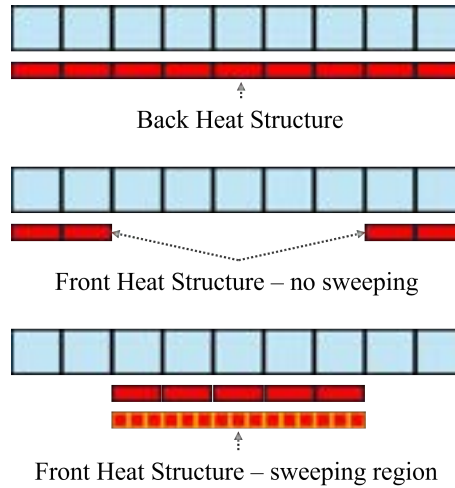


**Figure 3.37:** Sketch of the types of thermal structures implemented in TRACE model of PFC cooling circuit.

Regarding the front side portion, some observations must be made about the axial modelling strategy. Each VT has been equipped with two kinds of heat structures as shown in figure 3.37.

In particular, along the axial direction, two distinct types of structures have been defined as follows:

- the no sweeping portion: composed by the extremes of VT, where the sweeping effects are negligible and the heat flux, as described by the Eich profile in the case of attachment, is close to zero. The thermal structure here consists of a number of thermal cells corresponding to the hydraulic segmentation. Specifically, each VT is equipped with a single heat structure at each extreme, and axial conduction between cells is disabled;
- the sweeping portion: the region in which the effects of the sweeping application are evident. The region is broader than the maximum amplitude of sweeping so that in the case of loss of detachment, the Eich profile characterised by important heat flux is faithfully replicated. In this region, a thermal structure has been defined for each hydraulic cell and discretised in a number of sub-units to catch the effective linear heat flux of the VT. Additionally, axial thermal conduction is enabled between the sub-units of the thermal cells.

This modelling strategy has been adopted to accurately capture the distinct thermal behaviours observed along the axial direction of the VT.

In particular, the Eich profile is characterised by a sharp and localised peak, which results in sudden changes in heat flux conditions occurring over short time scales. Accurately resolving these changes requires a finer axial discretisation and the inclusion of axial thermal conduction, especially since the axial dimension is comparable to that of the real MB.

Conversely, in the no-sweeping regions, the heat flux is nearly zero and remains unaffected by the sweeping dynamics. Therefore, introducing additional complexity in those areas would not provide any significant benefit in terms of accuracy, while unnecessarily increasing the computational cost.

The adopted approach thus ensures a physically consistent and computationally efficient representation of the system.

### **3.3.2 Control system sub-model of the DEMO Divertor Vertical Targets in sweeping condition**

In order to account for the volumetric thermal load on the heat structures of the VTs, as well as the power deposition on the coolant due to neutron heating and decay heat during normal operation, a dedicated set of control systems has been implemented. These systems manage the evolution of thermal loads throughout the simulation, adapting them to the different operating phases.

A conceptual scheme of the implemented architecture is shown in figure 3.38. Based on the simulation time, the heat structures of the VTs are subjected to distinct volumetric thermal loads. Analogously to previous assumptions, the sweeping time and the detachment loss time are considered equivalent.

Specifically, both VTs can experience:

- normal condition ( $t_{\text{simulation}} < t_{\text{sweeping}}$ ): the heat structures are subjected to the nominal volumetric load, represented by a normalised profile that follows the duty cycle and is multiplied by a coefficient accounting for the thermal contribution from neutron heating and decay heat;
- loss of detachment and sweeping application ( $t_{\text{sweeping}} \leq t_{\text{simulation}} < t_{\text{shutdown}}$ ): the volumetric load remains at its nominal value corresponding to the pulse condition;
- plasma shutdown and sweeping application ( $t_{\text{simulation}} \geq t_{\text{shutdown}}$ ): the thermal contribution decreases linearly from unity to zero, simulating the ramp-down. Once the plasma is completely switched off, a decay heating trend is applied to the structures according to the calculations reported in [60].

This approach ensures that the heat structures of the VTs experience realistic thermal loads throughout the transient, while maintaining consistency with the physical phenomena and operational scenarios.

Concerning the volumetric heating of the coolant, when the simulation time reaches the shutdown time, the normalised profile decreases linearly from unity to zero, corresponding to the complete plasma switch-off.

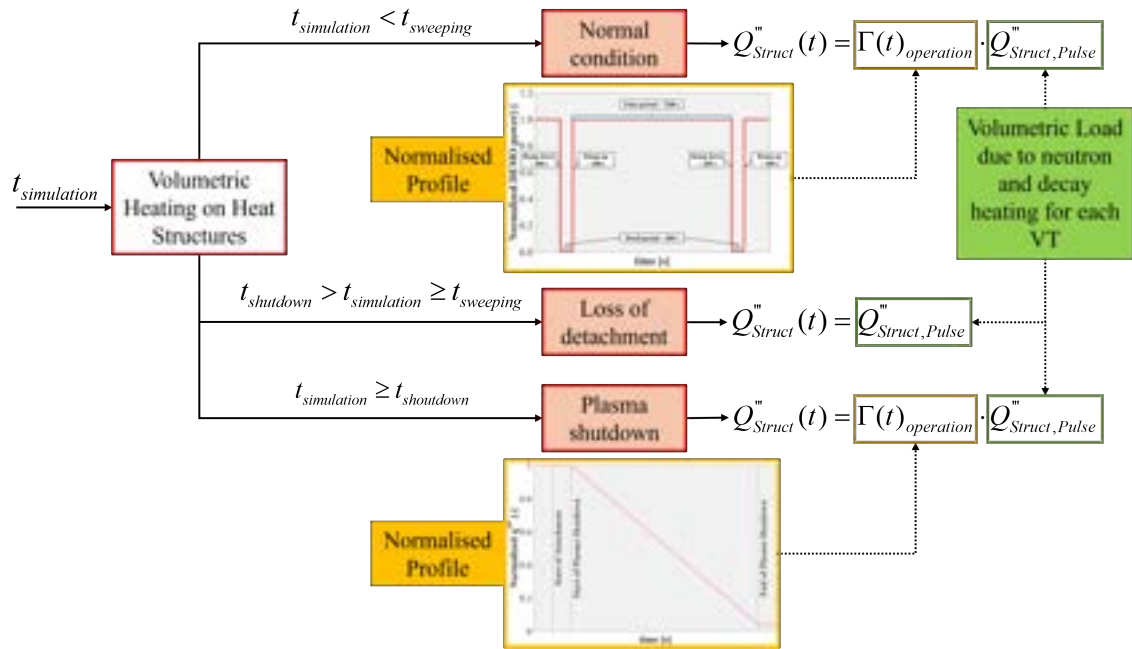


**Figure 3.38:** Schematic overview of the control system architecture to model volumetric loads.

### 3.3.3 Python and ECI tools for modelling DEMO Divertor Vertical Targets in sweeping condition

In analogy with section 3.2, the heat flux condition for each cell of the heat structure and its sub-units has been defined through dedicated Python scripts.

These scripts are provided step by step during the simulation to the TRACE model via the ECI driver.

During the normal operating conditions, the heat flux breakdown shown in figure 1.8a has been properly implemented in the VTs domain replicated in TRACE.

The implementation of Eich profile, which is supposed to occur during loss of detachment and its periodic movement across VTs caused by the adoption of the sweeping technique, is much more complex. Furthermore, the assumption of a plasma shutdown during the sweeping makes modelling even more difficult due to the presence of internal variables within the profile that are closely related to the shutdown process.

In particular, during the shutdown of the device, the plasma current as well as the power to the SOL are reduced. The current reduction could be supposed to be a linear decrease, as in the ramp-down according to [61, 22]. In particular, the current changes from the value reported in table 1.1 to zero over a defined time interval (equation (3.15)).

$$I_p(t) = I_{p,start} - \frac{I_{p,start}}{t_{end} - t_{start}} \cdot (t - t_{end}) \quad (3.15)$$

The same assumption of a linear decrease has been applied to the power at the SOL, which varies in the same time interval from the nominal value in table 1.1 to zero as reported in equation (3.16).

$$P_{sol}(t) = P_{sol,start} - \frac{P_{sol,start}}{t_{end} - t_{start}} \cdot (t - t_{end}) \quad (3.16)$$

Considering these aspects and building upon the equations presented in section 1.3.4 related to the attachment phase, the time-dependent variables affected during the plasma shutdown have been identified. Specifically, the total heat flux on the VT,  $q_0''$ , and the decay length  $\lambda_q$  vary over time and follow the proportional trends shown in equations (3.17) and (3.18).

$$q_0''(t) \propto \frac{P_{sol}(t)}{\lambda_q(t)} \quad (3.17)$$

$$\lambda_q(t) \propto q_{cyl}(t)^{1.11} \cdot P_{sol}(t)^{0.11} \quad (3.18)$$

As already observed  $q_{cyl}$  can be approximated to the safety factor  $q_{95}$ , which can be calculated taking into account [62, 22] as reported in equation (3.19), where  $r$  is the minor radius obtained taking into account the aspect ratio of the machine and the major radius  $R_{geom}$ ,  $\mu_0$  is the vacuum permeability and  $B_t$  the toroidal magnetic field.

$$q_{95}(t) = f_q(\kappa_{95}, \delta_{95}) \frac{2\pi r^2}{R_{geom}\mu_0} \cdot \frac{B_{tor}}{I_p(t)} \quad (3.19)$$

The function  $f_q(\kappa_{95}, \delta_{95})$  is related to the triangularity and elongation of the plasma, and it is supposed to be calculated as reported in equation (3.20) [63].

$$f_q(\kappa_{95}, \delta_{95}) = 0.5 \cdot \frac{1.17 - \frac{0.65}{A}}{(1 - A^{-2})^2} \cdot (1.0 + \kappa_{95}^2 \cdot (1.0 + 2.0\delta_{95}^2 - 1.2\delta_{95}^3)) \quad (3.20)$$

To ensure a consistent upper bound for the safety factor during the shutdown phase, a minimum plasma current threshold of 30 % of the nominal value has been imposed. Below this threshold, the safety factor is assumed to remain constant.

In this regard, figure 3.39 illustrates the time evolution of the Eich heat flux profile on the OVT during a 100 s ramp-down phase, assuming the sweeping mitigation procedure is inactive.

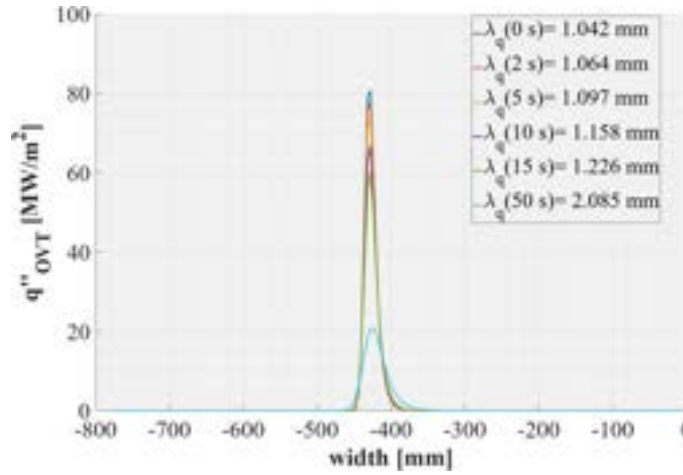


**Figure 3.39:** Time evolution of the Eich Profile on the TRACE model of the OVT without sweeping.

Although the sweeping is foreseen during loss of detachment to mitigate peak heat loads, it has been intentionally assumed inactive in this analysis to highlight the intrinsic flattening of the profile during plasma shutdown.

As time progresses from 0 s (when the plasma current and power start to decrease) to 50 s, two main effects can be observed:

- reduction of the peak heat flux: this is primarily driven by the decrease of  $q_0''(t)$ . The decrease in  $q_0''(t)$  results from its proportional dependence on both  $P_{\text{sol}}(t)$  and  $\lambda_q(t)$ , as expressed in equation (3.17). During the ramp-down phase,  $P_{\text{sol}}(t)$  decreases while  $\lambda_q(t)$  increases, so the combined effect inevitably leads to a marked reduction in the peak heat flux;
- broadening of the profile: the progressive spreading of the Eich curve is mainly governed by the increase in the decay length  $\lambda_q(t)$ , which results from the variation in plasma current and power during the shutdown phase, as described in equations (3.17) and (3.18).

Based on the treated modelling aspects, the PFC cooling circuit has been studied considering a set of transients.

### 3.3.4 Transient analyses and results of DEMO Divertor Vertical Targets

The defined TRACE model has been investigated, taking into account a series of transients divided as follows:

- transient 01: comparison among TRACE and ANSYS-CFX tools considering the sweeping of the Eich profile without the shutdown of plasma;
- transient 02: TRACE simulation considering the sweeping of the Eich profile with the shutdown of plasma in 100 s;
- transient 03: TRACE simulation considering the sweeping of the Eich profile with the shutdown of plasma in 200 s.

All transients have been conducted considering a sweeping of 1 Hz and an amplitude of 200 mm ( $\pm 100$  mm), and the swept Eich profile has also been replicated in both VTs. The thermal-hydraulic parameters discussed below refer to the OVT, which is the most critically stressed component during a loss of detachment event. The objective is to characterise the thermal-hydraulic behaviour of these regions under sweeping conditions, in order to identify the most critical areas of the OVT.

The analysis focuses on two specific regions:

- Region 01: the location corresponding to one of the extremes of the sweeping amplitude, expected to be the most critical region;
- Region 02: around the position of the SP under normal operating conditions.

The regions have been selected taking into account the average heat flux over the time in the VT which is reported in figure 3.40.

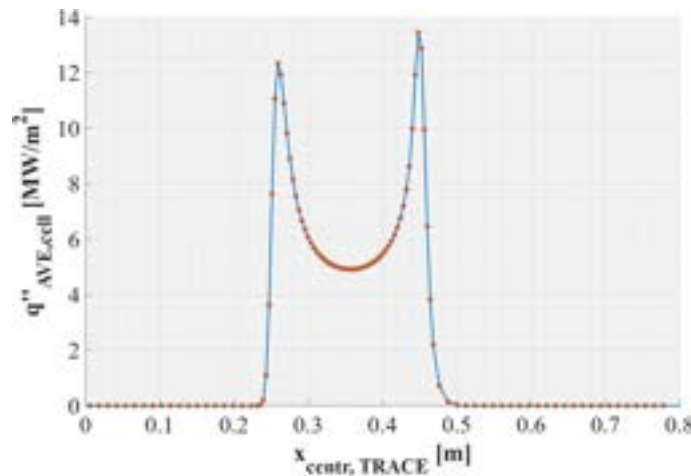


**Figure 3.40:** Axial distribution of the average heat flux over time in OVT considering the sweeping of the Eich profile.

In particular, the two peaks correspond to the locations where the SP reverses its direction due to the sweeping application. Region 01 corresponds to the equivalent MB with the highest time-averaged heat flux value, while region 02 lies within the concave section where the minimum occurs, highlighting the advantage of the sweeping technique.

#### **3.3.4.1 Transient 01: Comparative Analysis of TRACE and ANSYS-CFX Under Sweeping Conditions Without Plasma Shutdown**

To demonstrate the modelling consistency, the TRACE model has been initially simulated considering a sweeping transient and compared to analogous thermal studies conducted in ANSYS-CFX.

Specifically a TRACE transient has been set considering 30 s of no-power condition with adiabatic outer VTs surface and without volumetric thermal loads on them to assess a steady-state behaviour of the overall domain, subsequently the Eich profile has been applied as a heat flux condition on the heat structure of PFCs and immediately swept for a time interval of a 30 s.

While the ANSYS-CFX transient has foreseen 6 s of sweeping application in case of loss of detachment acting on a PFU fundamental unit in ANSYS-CFX. An initial condition without volumetric thermal loads and an initial temperature of all solid domains of 130 °C with a coolant velocity of 13 m/s to replicate the presence of the ST have been set up. The plasma face surface of W has been characterised by a heat flux condition where tabular values, periodically used, are provided. In particular, two periodic profiles have been adopted, considering the two distinct aforementioned regions.

Subsequently, the TRACE thermal cell, containing the sub-unit subjected to the highest heat flux on its outer surface, has been compared to the analogous transient performed on the 3D-PFU fundamental unit using ANSYS-CFX.

The outer boundary condition applied to both thermal analyses, respectively to the thermal subunit in TRACE and the PFU fundamental unit in ANSYS-CFX, is reported in figure 3.41.

The thermal ANSYS-CFX simulation has been conducted considering a HTC profile dependent on temperature and calculated by the use of the MATLAB routine [56].

In both codes, the Eich profile has been adopted, considering a constant all time-dependent parameter without a plasma shutdown.

Despite replicating the thermal-hydraulic behaviour of a large domain and simulating a more extensive transient, the simulation time of the TRACE model has required only 83 s, significantly shorter than the approximately 700 s taken by the ANSYS-CFX simulation for both regions.

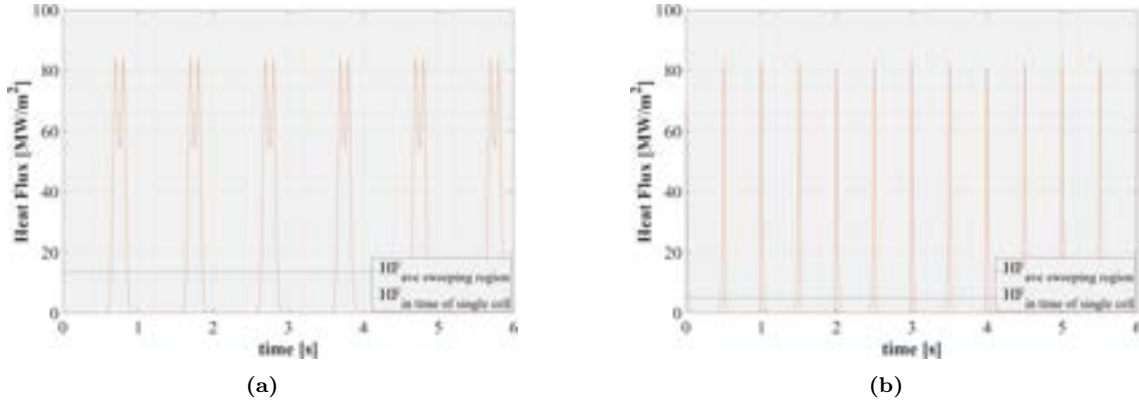


**Figure 3.41:** Time evolution of the thermal load on the outer surface of the ANSYS-CFX  $W$  domain, considering the sweeping of the Eich profile (orange curve), and the time-averaged value of the applied load (blue curve): Region 01 - extremes of the sweeping amplitude (a), Region 02 - initial position of SP (b).

Concerning the results, the relevant parameters are shown in figures 3.42 and 3.43.

Starting from the first region located at the edge of the sweeping range (figure 3.42), the results show that the behaviours among the codes are fairly similar. The energy content of the PFU (figure 3.42a) shows some discrepancies, although the error on the peak value is approximately 13%. This can be attributed both to numerical factors in the post-processing calculation of the energy content, which is not directly provided by the SYS Code, and to the possibility that the fraction of surface area actually involved in the thermal exchanges, used to derive the analytical geometry, is in fact smaller. Additionally, the other differences observed in figures 3.42b and 3.42c, which are still deemed acceptable, can be explained by the distinct treatments of the HTC, as already discussed in section 3.2.2. It is important to note that the linear regression model is able to reproduce the trend of the figure 3.42d even if connected to an Eich thermal profile.

The results associated with the second region, where the initial SP position is located, are characterised by a much higher consistency among the codes, as can be observed in figure 3.43. The overestimation of the energy content (figure 3.43a) can again be attributed both to numerical factors and to a smaller portion of the surface actually involved in thermal exchange in the case of a profile such as Eich. Nevertheless, the error on the maximum temperature of the layers decreases to below 10%. In general, the smaller discrepancies between the codes concerning the figures 3.43b and 3.43c can be explained by the lower time-averaged heat flux, as can be observed in figure 3.41b, which ensures lower wall heat flux and subsequently the single-phase flow with slight variations in the HTC. Once again, the regression model successfully reproduces the temperature behaviour of  $W$  (figure 3.43d).

Table 3.19 presents the relative error between the TRACE and ANSYS-CFX simulations, using the latter as the reference. All relative errors are below 8%, and the errors related to the peaks, which are more relevant, are all below 4%.

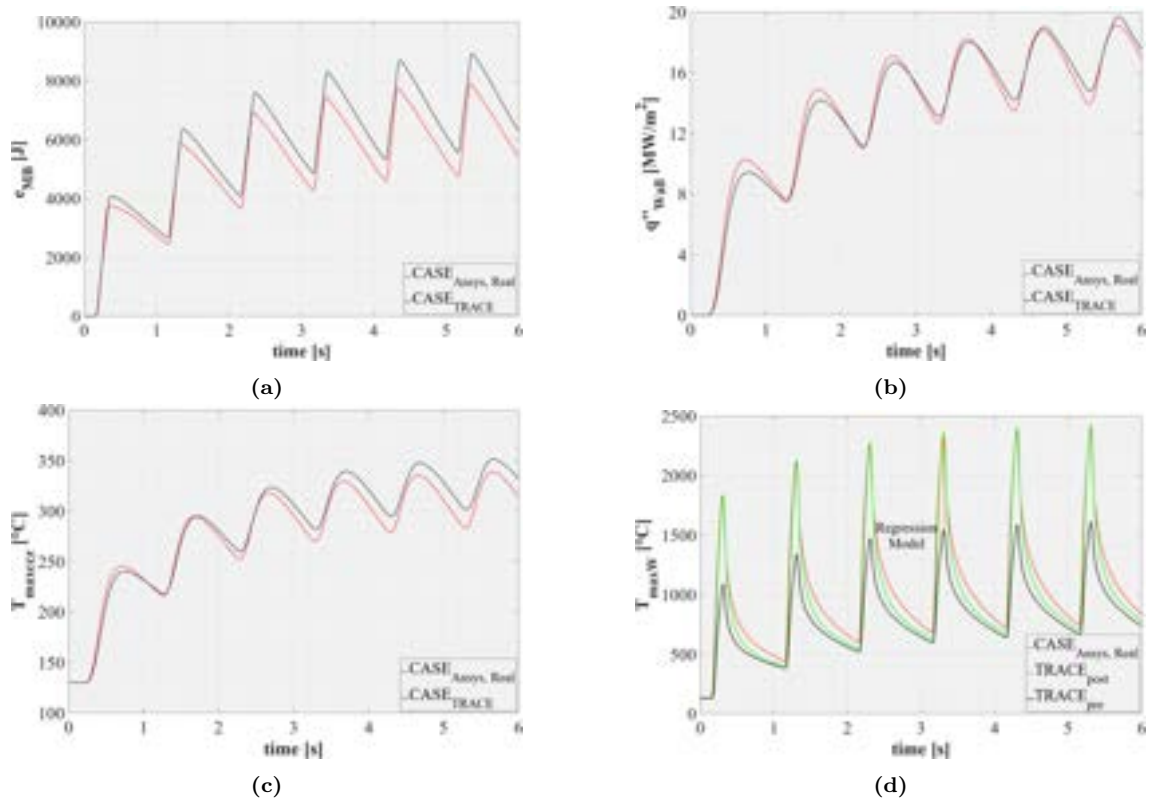


**Figure 3.42:** Sweeping of Eich profile without shutdown Region 01: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

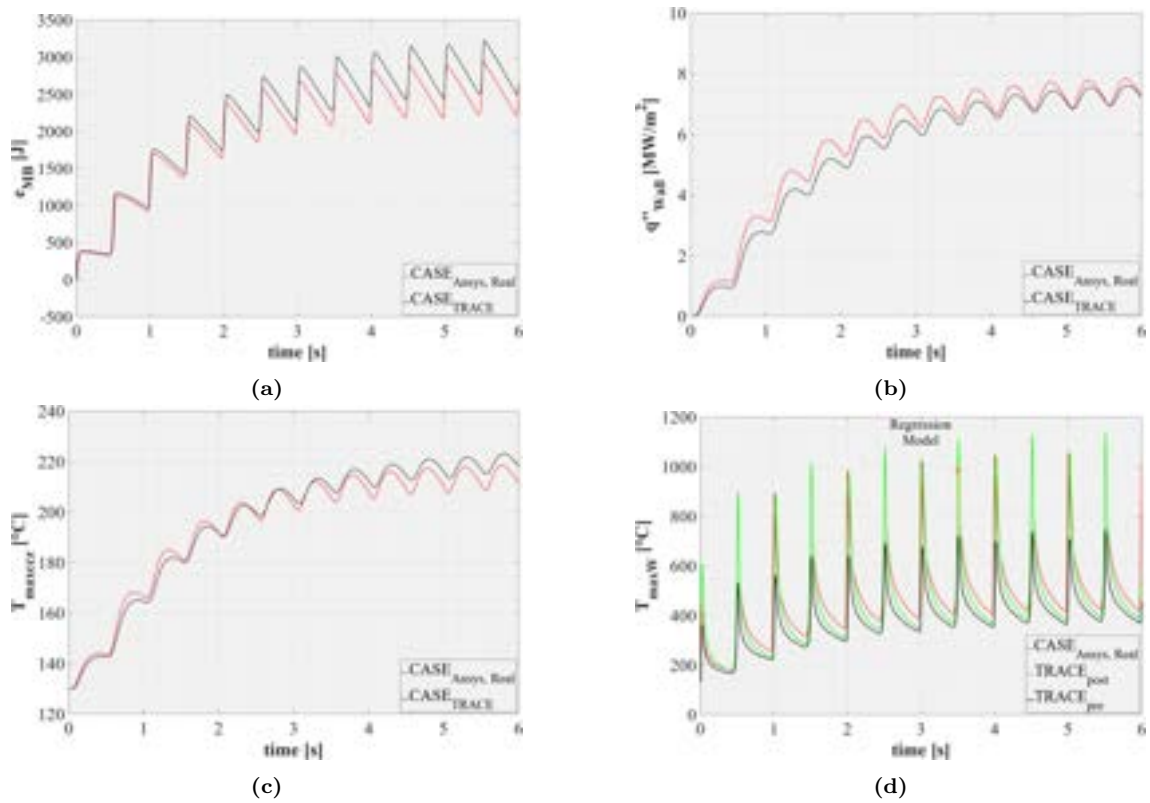


**Figure 3.43:** Sweeping of Eich profile without shutdown Region 02: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

Since these peak-related errors are consistently positive, it can be observed that the TRACE model tends to overestimate the maximum temperatures, thereby ensuring a conservative approach.

**Table 3.19:** *Percentage errors of TRACE simulations compared to ANSYS-CFX considering Eich profile without plasma shutdown.*

| $\epsilon_{\text{MIN}}/\epsilon_{\text{MAX}}$ | $T_{\text{W}}$ | $T_{\text{Cu}}$ | $T_{\text{CCZ}}$ |
|-----------------------------------------------|----------------|-----------------|------------------|
| <b>Region 01</b>                              | -6.23/1.58%    | 5.56/2.11%      | 5.82/3.32%       |
| <b>Region 02</b>                              | -7.67/1.29%    | 1.88/0.81%      | 2.59/1.93%       |

While the errors in W temperature are largely comparable across regions, the errors in the other layers vary from one region to another, once again due to differences in the time-averaged heat flux. This value is higher in the first region, as already observed, leading to a misalignment HTC between the codes which adopt distinct correlations.

Generally, it could be observed that the sudden application of the sweeping technique in case of loss of detachment without a plasma shutdown in the interval of 6 s ensures compliance with part of the requirements proposed in section 1.3.4 and the results. The results, taking into account the appropriate differences, are reasonably consistent with what has been stated in [22, 61]. Considering both regions, the design issue of CHF margin is satisfied. On the contrary, the recrystallisation requirements and the CuCrZr maximum temperature are not met in the first region, at the far end of the sweeping amplitude, corresponding to the axial reversal point of the strike point induced by the sweeping application.

It is therefore evident that, when sweeping occurs concurrently with attachment, the region requiring the most attention is not near the initial location of the SP where the Eich peak is first observed, but rather the PFUs positioned at the extremes of the sweeping amplitude, where the reversal of direction causes the peak to remain for a longer duration.

Based on the observations made, the modelling strategy and the regression model have been considered suitable for simulating more complex cases using TRACE, including those involving plasma shutdown.

#### **3.3.4.2 Transient 02: Transient Analysis under sweeping condition with a plasma shutdown in 100 s**

The TH model of the PFC cooling circuit has been further analysed over a 140 s transient, which includes 30 s of steady-state operation under normal DIV conditions. At the end of this interval, a sudden loss of detachment occurs, during which the sweeping technique is applied. Plasma shutdown is assumed to take place 10 s after the sweeping, followed by a 100 s ramp-down phase to switch off the device.

The sweeping technique is assumed to be applied throughout the entire ramp-down phase.

The following results (figures 3.44 and 3.45) for both regions are related only to the first 30 s of sweeping to show clearly the parameter trend. It is worth observing that, in addition, after 50 s from the start of plasma shutdown, the outer heat flux, properly scaled on the real 3D-MB, decreases and reaches a value of 20 MW/m<sup>2</sup> below which the sweeping can be interrupted. This threshold corresponds to the allowable peak heat flux density for slow transients, below which the thermal load remains within the qualified operational range for DEMO MB [13].

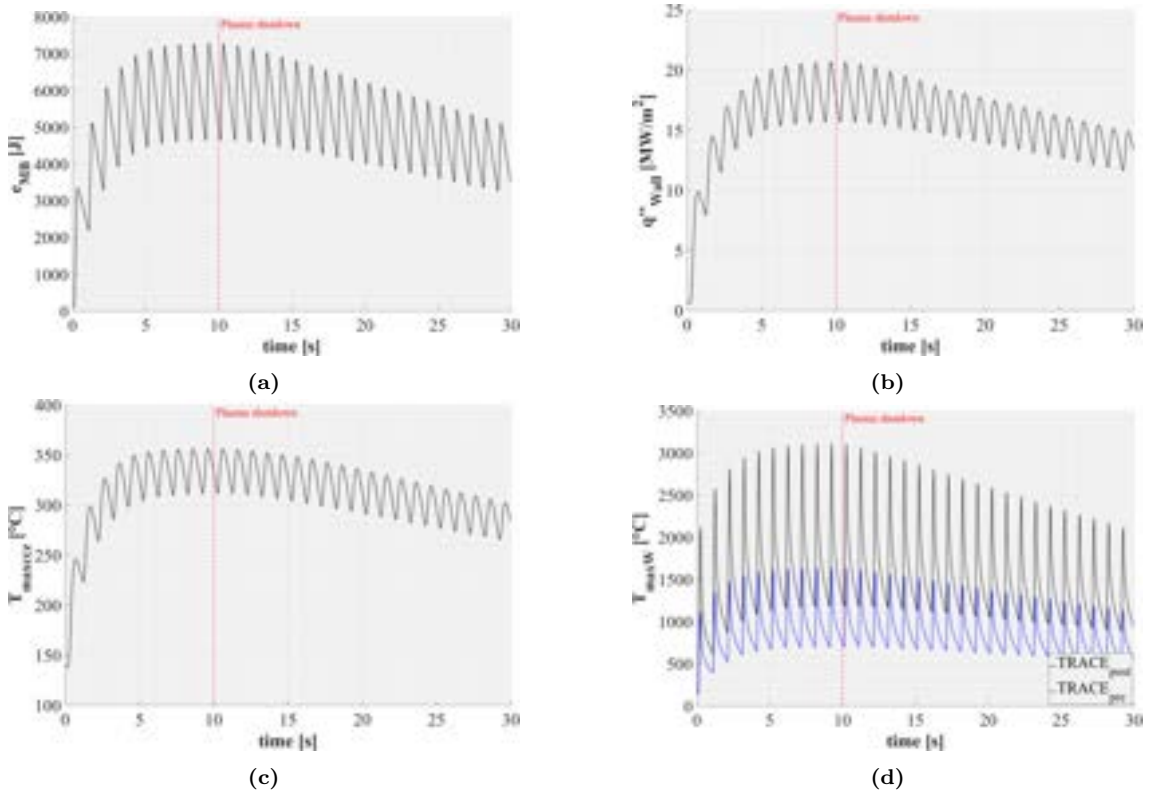


**Figure 3.44:** Sweeping of Eich profile Transient 02 Region 01: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

Concerning the results for the first region shown in figure 3.44, it can be observed that the requirements regarding recrystallisation and CuCrZr temperature are not fulfilled. The maximum CuCrZr temperature drops from the limit after 5 s from the plasma shutdown.

Additionally, the results indicate that the plasma shutdown occurs too late without having any beneficial contribution. Assuming the non-binding limit on recrystallisation, the observed trends suggest that the manoeuvre should be anticipated by at least 4 s after the loss of detachment in order to prevent the CuCrZr from reaching its peak temperature.

The PFU that hosts the SP during the normal operation condition and at the

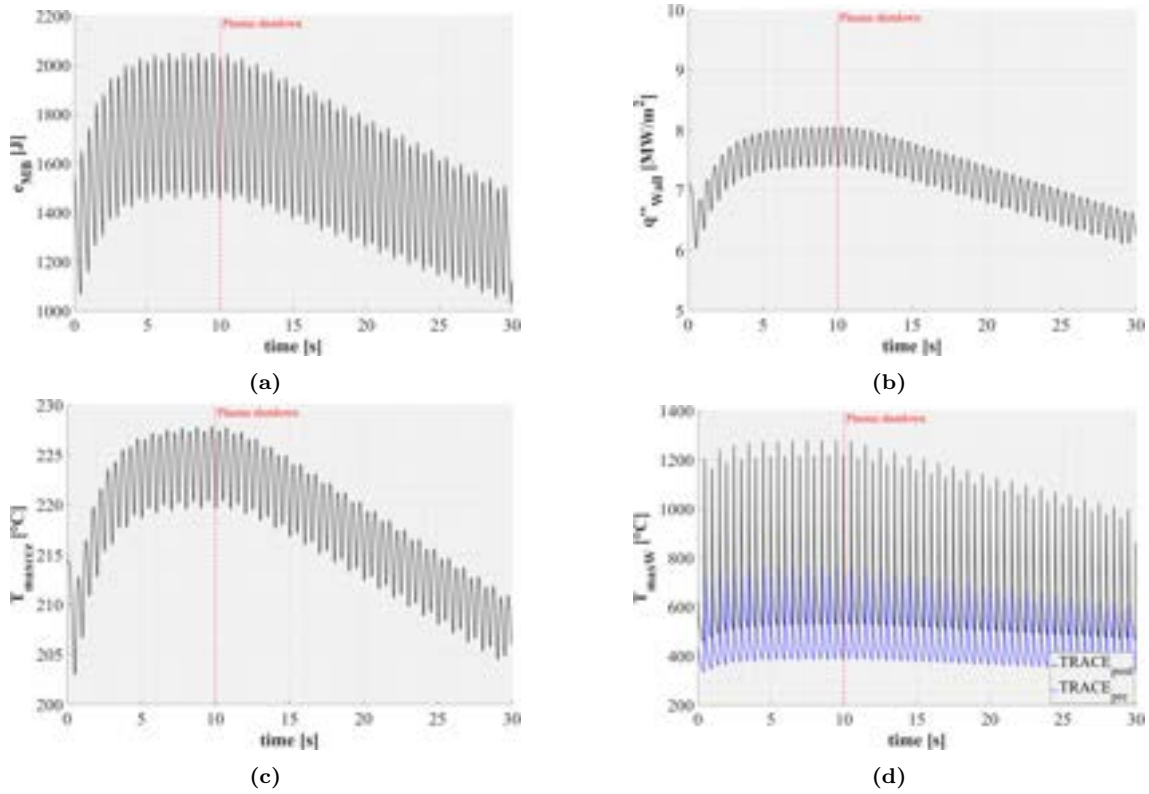


**Figure 3.45:** Sweeping of Eich profile Transient 02 Region 02: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

initial phase of the loss of detachment figure 3.45, responds well to the sweeping, except in cases where W temperatures exceed the recrystallisation threshold.

Generally, in accordance with the ramp, the profile of all quantities exhibits a decreasing trend in both regions, and it has been observed that sweeping can be stopped after the first 50 s of the ramp-down phase. Once again, the first region proves to be the most critically stressed area.

### 3.3.4.3 Transient 03: Transient Analysis under sweeping condition with a plasma shutdown in 100 s

An extended 240 s transient has been analysed, analogous to the scenario discussed in section 3.3.4.2, with the only difference being the duration of the ramp-down phase, which is extended to 200 s while maintaining active sweeping throughout.

To highlight the evolution of key parameters, the analysis focuses on the first 30 s of sweeping. Notably, after 100 s from the start of plasma shutdown, the outer heat flux, scaled to the actual 3D-MB geometry, drops below 20 MW/m<sup>2</sup>, allowing the sweeping to be safely interrupted.

The results, illustrating the behaviour during the first 30 s and the impact of the prolonged ramp-down phase, are presented in figures 3.46 and 3.47.

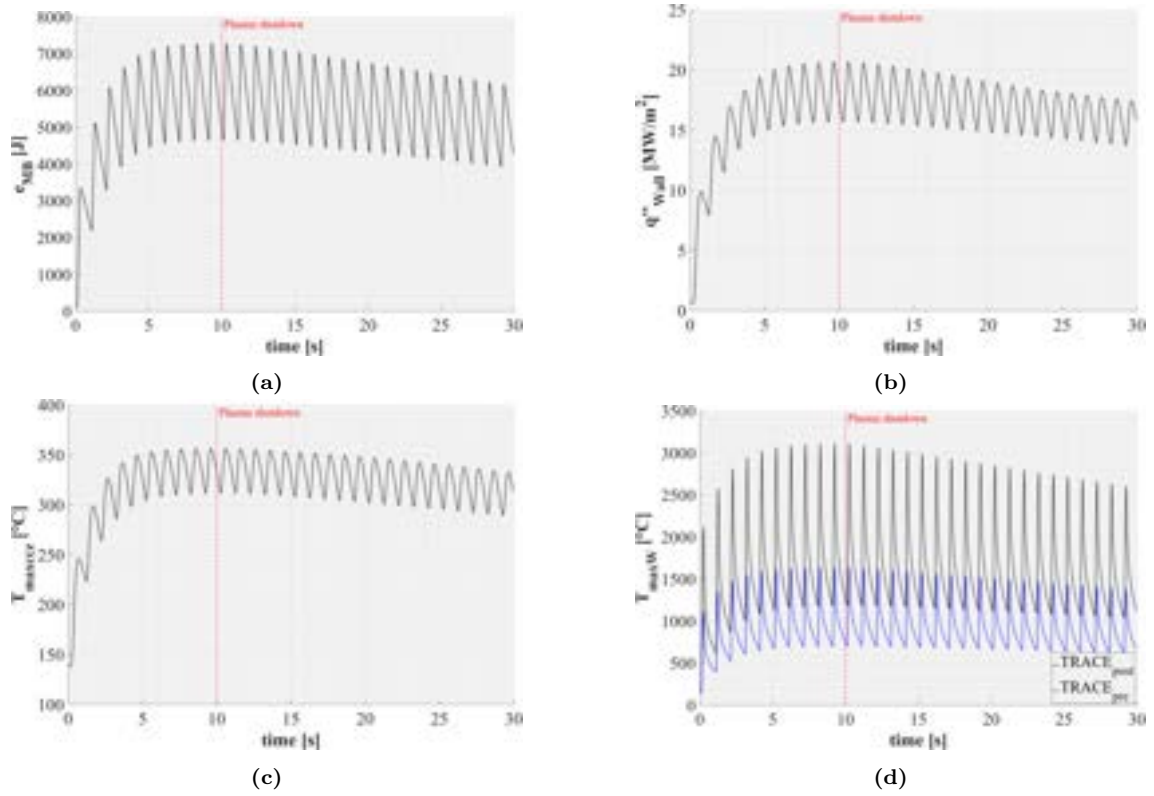


**Figure 3.46:** Sweeping of Eich profile Transient 03 Region 01: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

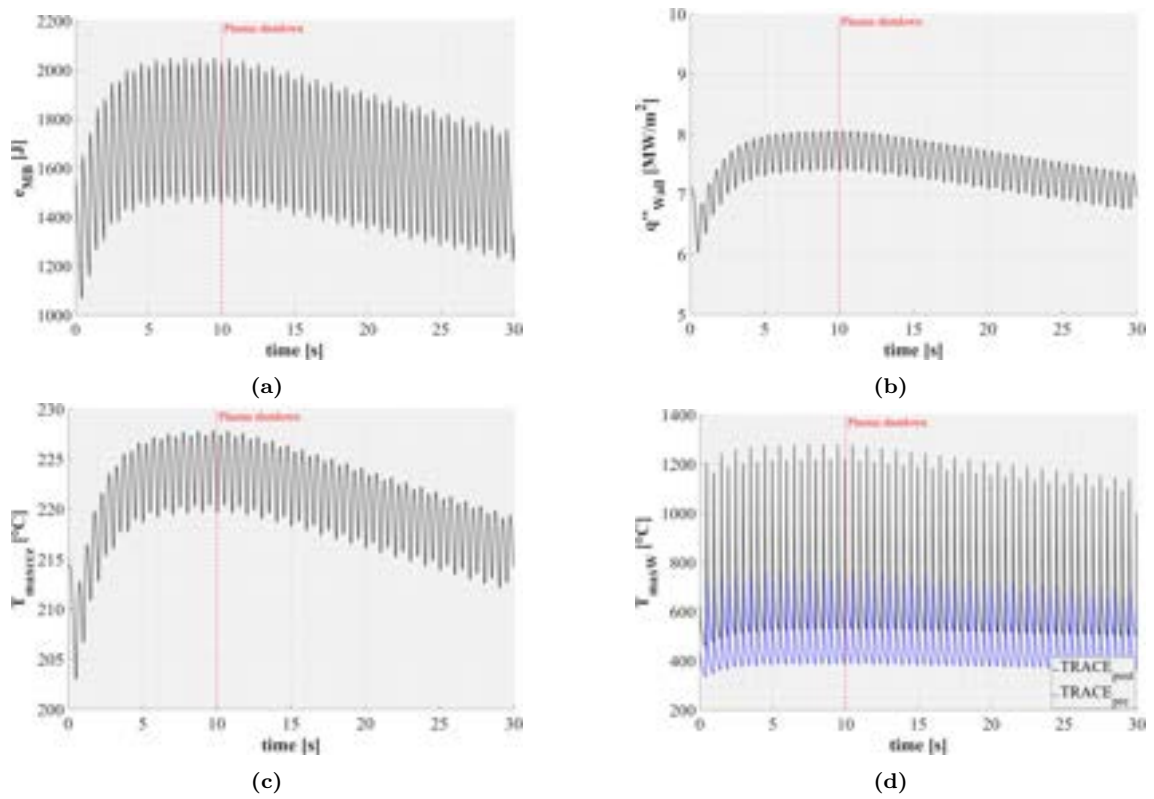


**Figure 3.47:** Sweeping of Eich profile Transient 03 Region 02: Energy content of the PFU fundamental unit (a), Coolant heat flux (b), CuCrZr (c) and W (d) maximum temperature.

As shown in figure 3.46, Region 1 remains the most thermally stressed area. The CuCrZr temperature exceeds the limit of 350 °C, both before and shortly after shutdown, only falling below the limit approximately 7.5 s later.

Assuming a non-binding recrystallisation limit, the ramp-down manoeuvre should be similarly anticipated by at least 4 s after detachment loss to prevent excessive CuCrZr heating.

The PFU hosting the SP during normal operation and the early detachment phase (figure 3.47) shows a generally favourable response to sweeping, except in cases where W temperatures exceed the recrystallisation threshold.

Compared to the section 3.3.4.2, the longer ramp-down results in a slower thermal response, as reflected in the temperature and heat flux profiles. Overall, all quantities exhibit a decreasing trend, and sweeping can be safely stopped after the first 100 s of the ramp-down phase.

### **3.4 Concluding remarks on the DEMO Divertor Vertical Targets in sweeping condition**

The modelling strategy developed for the DEMO DIV VTs under sweeping conditions is the result of a structured, multi-step workflow that combines optimisation algorithms, Python scripting, regression models, and the TRACE system code via the ECI driver.

This integrated approach has enabled the definition of an equivalent thermal structure that reproduces the dynamic behaviour of the real PFU fundamental unit with good fidelity, while ensuring computational efficiency and physical consistency.

The multilayer cylindrical geometry has been first obtained through optimisation and then refined using analytical methods and linear regression techniques. This combination proved effective in capturing both the energy content and the transient thermal response of the MB. The use of Python scripts in conjunction with the ECI interface has also simplified the implementation of complex, time-dependent heat flux profiles, such as the Eich distribution, ensuring accurate boundary condition management across the simulation domain.

The results confirm the robustness of the methodology, especially in loss of detachment events and the associated sweeping mitigation strategy. Comparisons with reference studies further support its validity.

Under the assumptions made, a real PFC in-vessel cooling circuit has been simulated, taking into account plasma interaction with the VTs during a loss of detachment event. In particular, the model reproduces the application of a swept Eich profile across VTs.

The results demonstrate the effectiveness of the sweeping strategy and highlight the thermal challenges encountered, especially in the regions corresponding to the

reversal points of the SP trajectory (as defined in section 3.3). These areas are subject to higher time-averaged heat fluxes, which may lead to exceeding the maximum allowable temperature for CuCrZr and the recrystallisation threshold of W.

Despite these challenges, the modelling framework has been shown to be a useful tool for supporting design evaluations. Thanks to its modular structure and Python integration, it can be easily adapted to include different heat flux profiles or to perform parametric studies. This makes it a practical and flexible solution for analysing the cooling circuit of the DIV and assessing the thermal behaviour of the MBs.

## Chapter 4

# Thermal-hydraulic analyses of the Primary Heat Transport System of the Divertor Plasma Facing Component during In-Vessel and Ex-Vessel Loss of Coolant Accident

This chapter presents the thermal-hydraulic behaviour of the overall DIV PFC PHTS under accidental transient conditions. It begins with a description of the TRACE model set-up, including the main components, modelling assumptions, and features necessary to simulate the selected transients.

The second part of the chapter focuses on the analysis of two specific accidental scenarios:

- the in-vessel LOCA: a cold feeding pipe of a single in-vessel PFC cooling circuit is subjected to a Double-Ended Guillotine (DEG) break during the pulse phase of the duty cycle with an instantaneous release of the coolant in the plasma chamber and the sudden plasma shutdown;
- the ex-vessel Large LOCA: a DEG rupture occurs in one of the cold legs of the loop during the pulse phase, causing a coolant release into the building. Two distinct rupture localisations are investigated, and in the first case, a sensitivity analysis is performed considering different plasma shutdown ramp profiles.

The objective is to demonstrate the effectiveness of the safety systems (where implemented), identify the key parameters to be considered during such accidental events, and provide guidance for their management.

## 4.1 General overview of the extended TRACE model

Taking into account the features of the PHTS described in section 1.4, a collapsed TRACE model of the entire loop has been developed. Additionally, the VVPSS has been replicated to enable the investigation of in-vessel LOCA scenarios.

The extended thermal-hydraulic model includes a significant number of components, as summarised in table 4.1, and a total of 384 control systems, divided into Trip Data, Control Blocks, Signal Variables, and General Tables.

**Table 4.1:** *Summary of TRACE components used in the thermal-hydraulic model.*

| Component    | Number     |
|--------------|------------|
| PIPE         | 169        |
| HTSTR        | 60         |
| POWER        | 5          |
| FLPOWER      | 4          |
| VALVE        | 36         |
| PUMP         | 2          |
| BREAK        | 10         |
| TEE          | 18         |
| FILL         | 4          |
| VESSEL       | 1          |
| <b>Total</b> | <b>309</b> |

### 4.1.1 Model setup of the Primary Heat Transport System of the Divertor Plasma Facing Component and related safety system

The collapsed loop of the PHTS feeding the DIV PFCs is illustrated in figure 4.1. To simplify the modelling procedure and reduce computational time, a collapsed layout has been adopted. Each half-loop consists of one detailed in-vessel cooling circuit representing a single cassette, while the remaining 23 cooling circuits have been merged into an equivalent pipework. This configuration has been applied symmetrically to both half-loops. The decision to isolate one cooling circuit per loop allows the TH model to simulate in-vessel LOCA transients with sufficient detail, as described in section 1.5. The in-vessel PFCs cooling circuit (see figure 3.35) has been modelled according to section 3.3, with an axial discretisation coarsening associated with a thermal de-refinement, since a fine resolution is not necessary for transient analyses in the absence of rapid variations or sharply peaked heat flux profiles in the equivalent cylindrical PFUs.

Concerning the ex-vessel portion, the modelling has been based on the detailed design of the systems reported in [36]. The flow domain has been discretised while

preserving the volume of each component, ensuring an accurate representation of the total coolant inventory. The sub-volumes have been appropriately positioned to reflect their relative locations and elevations. Additionally, concentrated hydraulic resistances have been modelled by calculating the loss coefficients according to [64], and appropriate wall roughness values have been assumed.

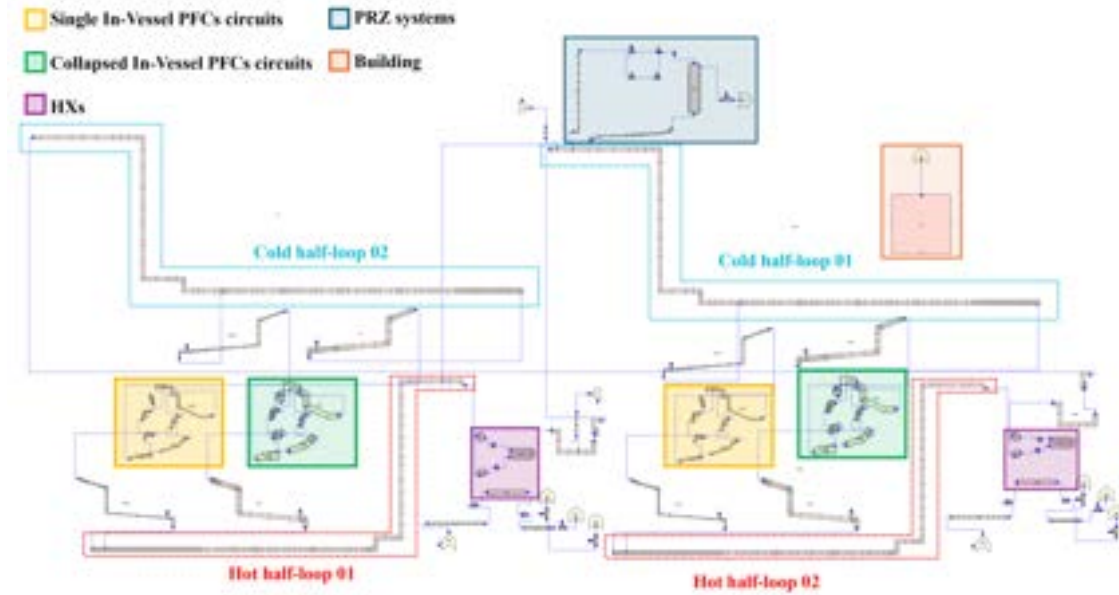


**Figure 4.1:** *SNAP view of the TRACE model of the DIV PFCs PHTS.*

A stand-alone volume of  $10^5 \text{ m}^3$  simulating the Tokamak Cooling Room (TCR) has been defined, filled with air at a temperature of  $50^\circ\text{C}$  and atmospheric pressure.

The hot/cold feeding pipes have been modelled as VALVE components, while the cold distributor/hot collector connected directly to the cold/hot leg has been modelled as a PIPE component. As shown in figure 4.1, the hot half-loop 01 is connected to the PRZ surge line (PIPE component), while the cold half-loop 01 is connected to the PRZ spray line (PIPE component).

A focus of the pressure control system consisting of the PRZ with associated piping and valves is depicted in figure 4.2 and replicates the design parameters reported on table 1.5. The PRZ is equipped with Safety Relief Valves (SRVs) to limit the maximum pressure reached within the PHTS and with proportional and backup heaters. The spray line branches into an emergency spray and a continuous spray. All related control systems are implemented according to the control rationale described section 1.4.3.

In the event of a LOCA, in analogy with NPP, non-safety-related control systems, such as the heater and the spray line controls, are assumed to be deactivated.

The two HXs and pumps have been modelled respectively in accordance with tables 1.4 and 1.6 and are shown in figure 4.3.

Both HXs sides are replicated by PIPE components. The tube side, where

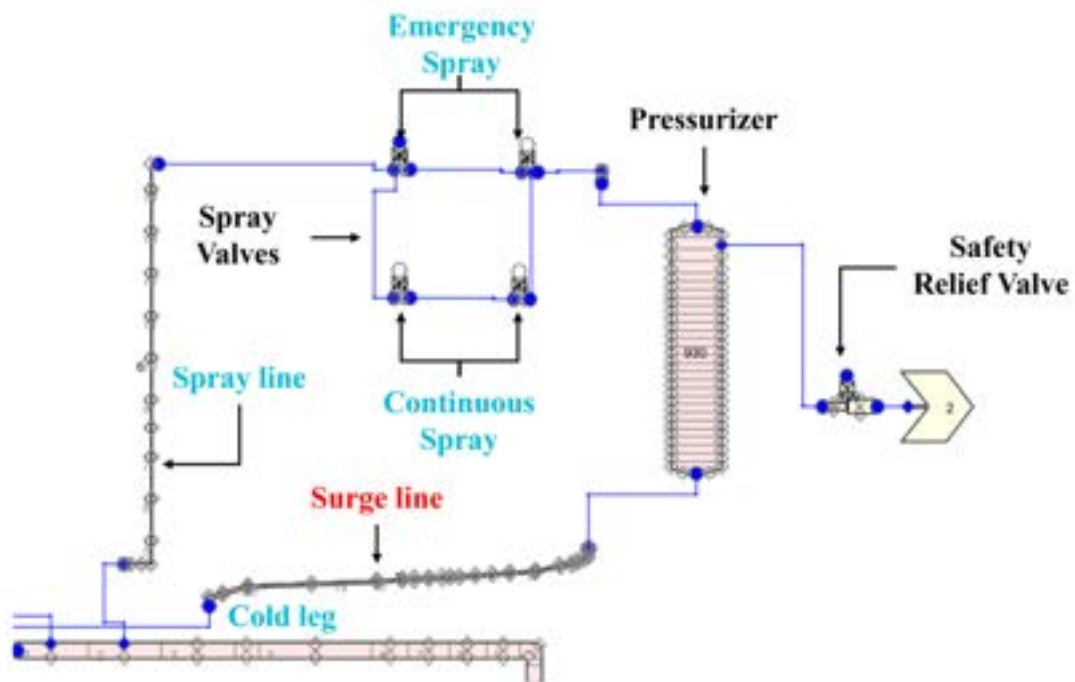


Figure 4.2: SNAP view of the TRACE model of the PRZ.

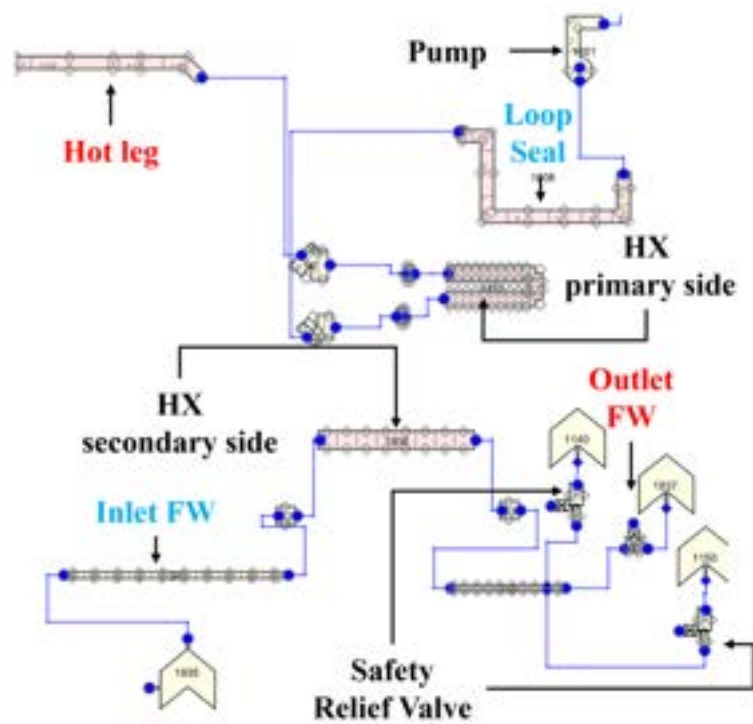


Figure 4.3: SNAP view of the TRACE model of the U-tube HX and the pump.

the primary coolant flows, has been represented by a single PIPE component characterised by a number of parallel pipes to reproduce the TH behaviour of the tube bundle. The shell side has also been modelled using a PIPE component, designed to replicate the cross-flow path of the secondary coolant. In this case, the model preserves the total volume of the shell side rather than its exact geometric configuration, as the focus is on capturing the correct flow path and TH behaviour rather than detailed geometry.

The HXs secondary side is connected to an inlet/outlet line simulating the Feed-Water (FW) coolant circuit. The outlet is equipped with SRVs to limit pressure excursions on the secondary side of the HX and an isolation valve that closes upon a turbine trip signal.

The pumps are modelled as a PUMP component starting from the input reported in table 1.6, adopting the Westinghouse characteristic curves already implemented in TRACE since a detailed design of the component is still not available in [36]. In order to simulate the pump during the LOCA, the pump speed coast-down (figure 4.4) curve has been calculated and implemented according to [65].

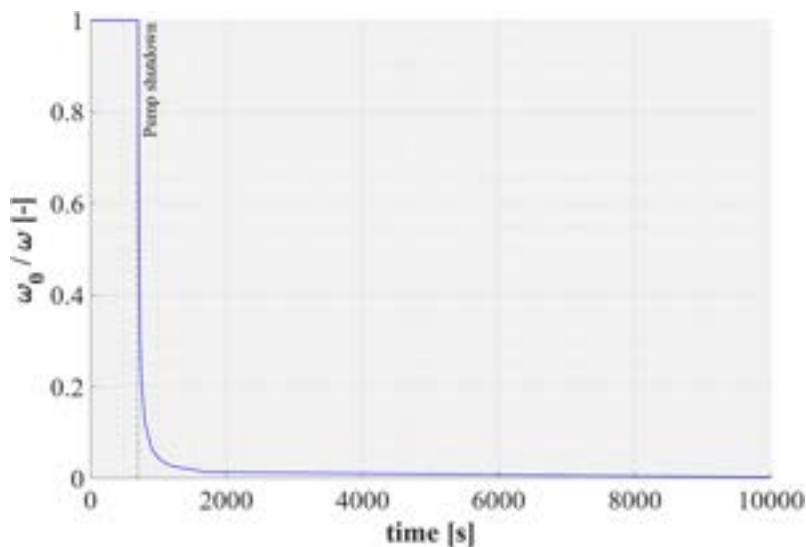


**Figure 4.4:** *Pump coast-down profile.*

Additionally, the TRACE model has been extended in order to consider the presence of the VVPSS, described in section 1.6, which is essential to manage the in-vessel LOCA.

In order to connect the PHTS to VVPSS, the VV has been properly modelled to realistically reproduce the coolant flow path to the VVPSS in case of an in-vessel LOCA. A total volume of 7130 m<sup>3</sup> has been considered, taking into account the effective geometry of VV by the use of the available CAD file provided by the EUROfusion DEMO Central Team (DCT).

The figure 4.5a shows the TRACE model of the VV, composed of 16 sectors. The figure highlights the plasma chamber, the upper and lower port regions.

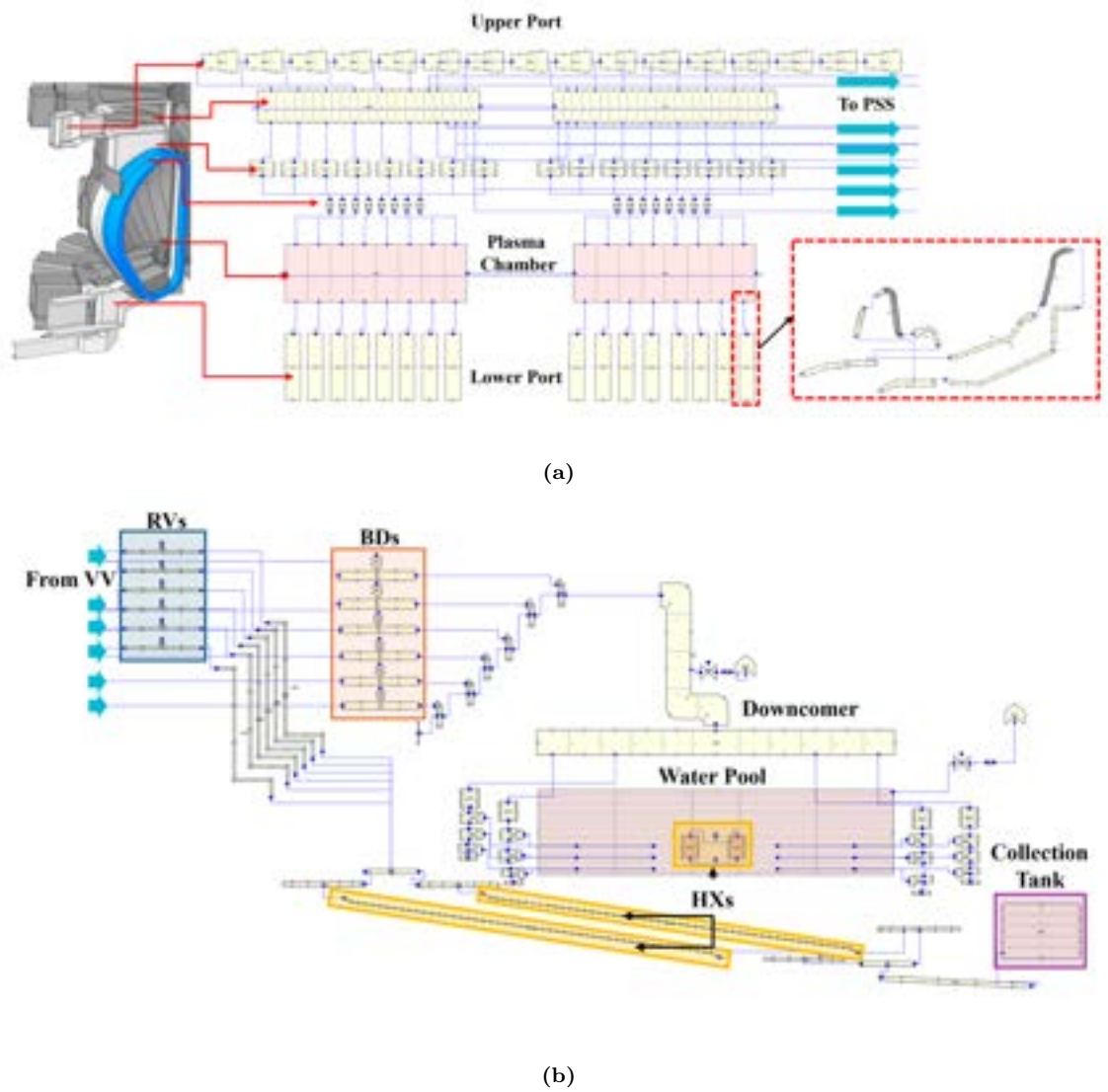


**Figure 4.5:** TRACE model details: focus on VV (a) and VVPSS (b).

In the model, small volumes have been introduced between the plasma chamber and the upper port regions to explicitly represent the narrow gaps between BB segments. These interspaces simulate the geometrical effect of the BB, from which the vapour produced in the plasma chamber during an in-vessel LOCA can pass through to reach the upper port region, where it is then managed by the VVPSS. In addition to this geometrical modelling of the BB, which is crucial to realistically simulate the vapour flow path within the VV, the thermal inertia of its plasma-facing and adjacent components, namely the First Wall (FWall) and the Side Wall (SW), has also been considered. These components, which thermally surround the plasma chamber, store a significant amount of energy during normal operation. In the event of an in-vessel LOCA, this stored thermal energy can be rapidly transferred to the leaked coolant, enhancing vapour production. This increased vapour generation may lead to a pressure rise within the VV, which is a critical aspect for the activation and performance of the VVPSS. This interaction has been explicitly modelled in

TRACE to capture its impact on both vapour generation and pressure evolution, ensuring a more accurate representation of the transient.

A HTC condition at the outer surface of both thermal structures has been defined, considering a profile of the coefficient that changes in time due to the pump coast-down, which is applied in the case of in-vessel LOCA as reported in figure 4.6. The sink temperature has been supposed to be equal to the bulk temperature, namely 311.5 °C.

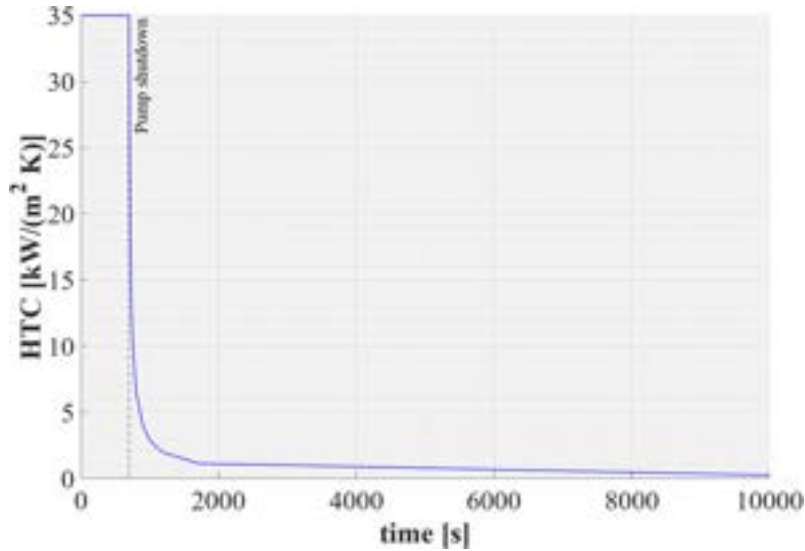


**Figure 4.6:** *HTC profile related to FWall and SW.*

With regards to the VV initial conditions, a pressure of 775 Pa has been assumed. This value does not reflect a physically accurate vacuum condition, but rather corresponds to the lowest pressure that could be set without triggering numerical instabilities or fatal errors in the TH code. Although not representative of the actual operational vacuum of the VV, this pressure is sufficiently low to ensure that the initial mass and internal energy are negligible compared to the mass and energy released during the transient. As a result, the influence of the initial conditions is limited to the very early stages of the simulation.

The upper ports of the VV accommodate the set of pipelines equipped with BDs and RVs, which are required for the management of accidental transients. These pipelines, as reported in figure 4.5b, direct the vapour towards the water pool of the VVPSS, as described in section 1.6.

The TH model of the VVPSS safety system has been provided by the DCT. The six RVs are characterised by the same flow area, equal to 0.076 m<sup>2</sup>, and are operated in pairs based on the overpressure experienced inside the VV. Specifically, three setpoints have been defined: 75, 85, and 95 kPa.

The six BDs, on the other hand, are sized in pairs: two have a flow area of 1.1 m<sup>2</sup>, while the remaining four have a flow area of 0.78 m<sup>2</sup>. All six BDs are associated with the same overpressure setpoint of 1.05 MPa.

As described in section 1.6, the minor leaks are managed by the progressive opening of RVs according to the pressure experienced at the upper part of VV. In this case, the vapour flows through the pipes, is subsequently condensed in the HXs, which are submerged in the water pool, and is finally collected in a 560 m<sup>3</sup> tank.

Major releases, on the other hand, are handled by the BDs rupture. The vapour condensates directly in the water pool after flowing through the downcomer section. The pool water has been reproduced by the use of a 3D-VESSEL component.

Based on the type of LOCA, a set of signals has been defined to determine the reactor trip, as well as the isolation of the HXs, the coast-down of pumps and the shutdown of the plasma (in the case of an ex-vessel LOCA). Analogously to the section 3.3.2, a decay heat power has been defined for the PFCs thermal structures. In both cases, the reactor trip signal is linked to a low-pressure signal. Specifically, the defined signals include both the low pressure at the PRZ and the low differential pressure across the in-vessel components and between the hot and cold legs of the loops. Their location is presented in figure 4.7.



**Figure 4.7:** *Signals location for the reactor trip.*

The low differential pressure signals have been adopted exclusively for the ex-vessel LOCA, since it has been observed that the signal in the PRZ does not allow timely identification of the transient accident.

## 4.2 In-vessel LOCA transient and results

As reported in section 1.5, the DIV PFC PHTS can experience in-vessel LOCAs. In this paragraph, the consequences of a DEG rupture of the cold feeding pipe related to one PFC cooling circuit will be investigated, which is located in the lower port of VV as shown in figure 4.8.

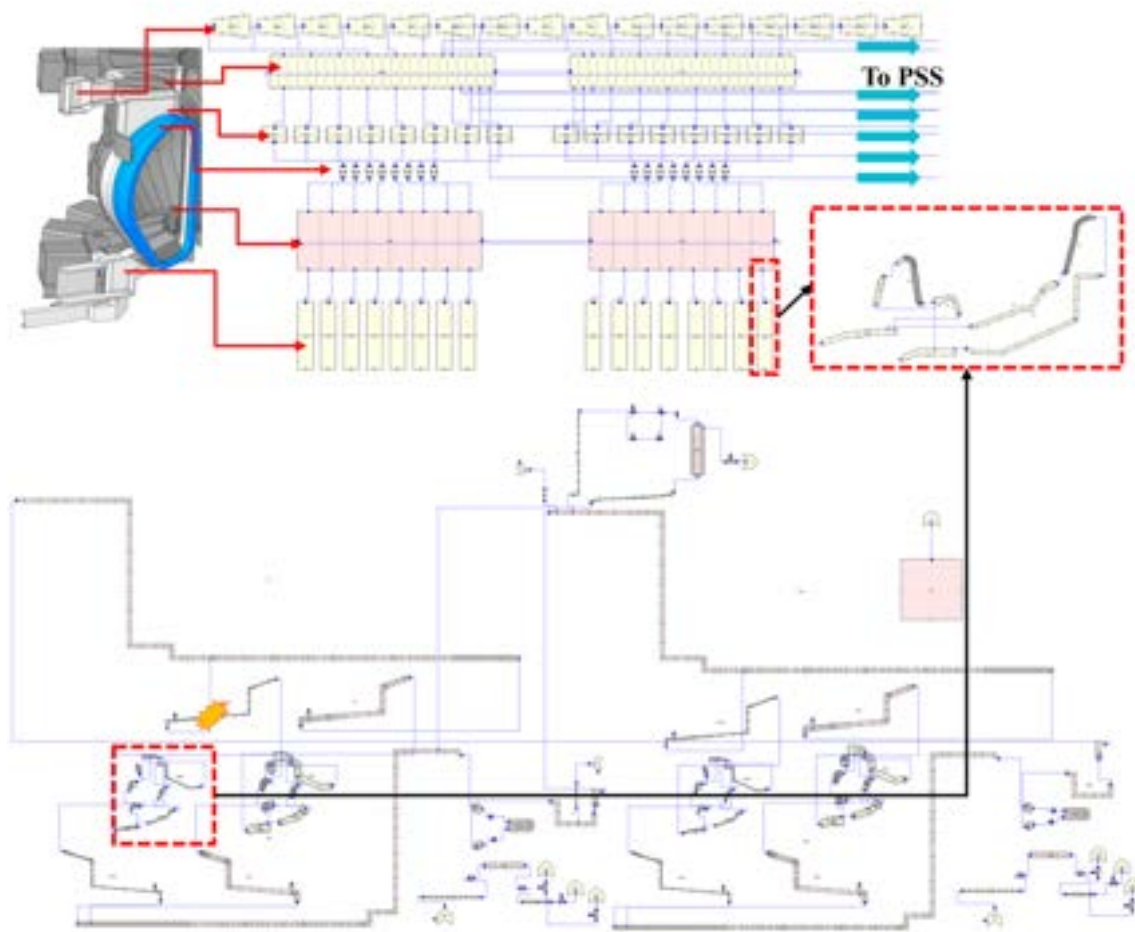


**Figure 4.8:** Cold feeding pipe DEG break location.

The DEG rupture is supposed to happen during the pulse phase of the duty cycle (figure 1.3) according to the severe power breakdown reported in figure 1.7b. Simultaneously with the event, the plasma is supposed to be immediately shutdown due to the presence of water inside the plasma chamber, leaving decay heat as the sole remaining thermal source as supposed in figure 3.38. Due to the lack of detailed information, the potential occurrence of fast plasma transients, which may determine additional heat loads on the in-vessel components, is neglected. All non-safety-related control systems are deactivated upon the LOCA, and the reactor trip is triggered by a low-pressure signal from the PRZ, which isolates both HXs and initiates the pump coast-down.

The main objective is to assess whether the VVPSS system is capable of managing the transient phase, ensuring the VV pressure remains below the specified limit of 200 kPa.

The simulation includes 1500 s of steady-state conditions related to the pulse phase. At 1501 s, the DEG event occurs and the coolant from a single PFC cooling circuit is released in the lower port of the VV, where it vaporises. Based on the

VV pressure, the RVs and BDs are opened to release the vapour in the VVPSS. The transient is simulated for an additional 8000 s to allow for a comprehensive assessment of the TH variables of the overall system.

The results presented here cover only the first 4000 s following the DEG rupture, as the relevant variables exhibit sufficiently stabilised behaviour within this timeframe.

Starting from the VV, as shown in figures 4.9a and 4.9b, only the two valves corresponding to the first setpoint (75 kPa) are activated.

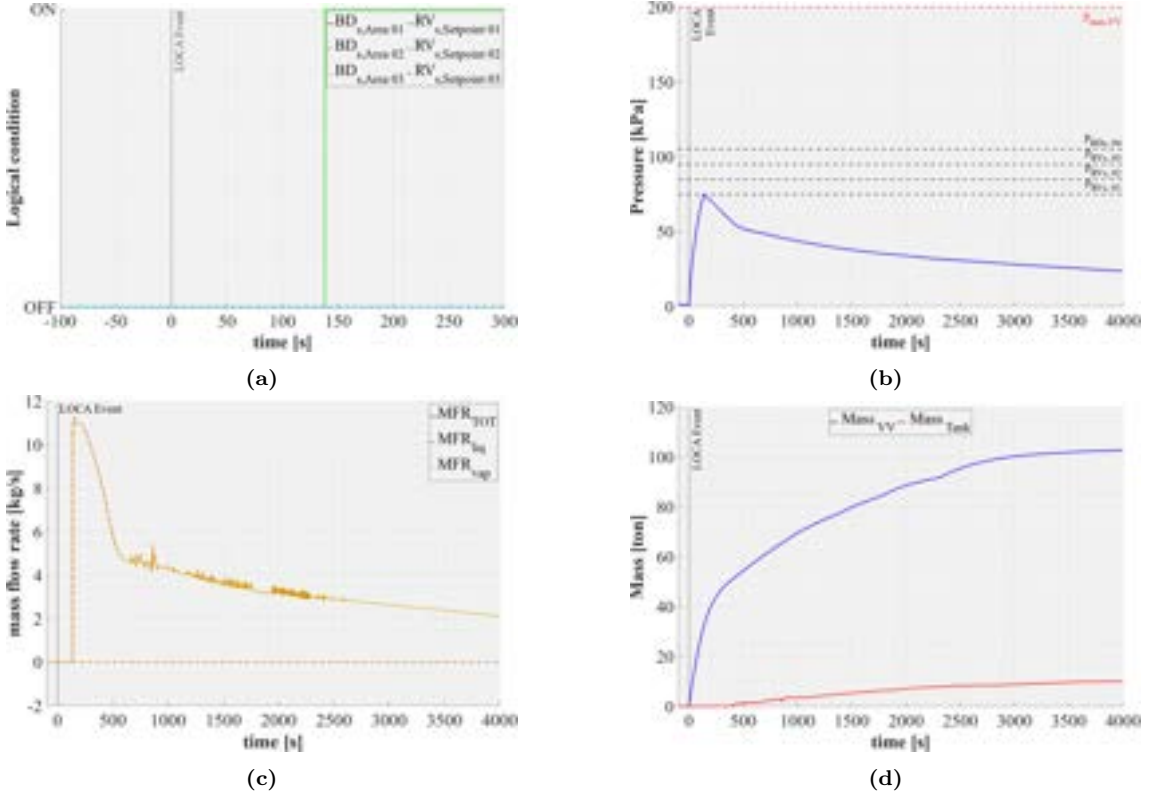


**Figure 4.9:** VV fundamental parameters during in-vessel LOCA: sequence of valves opening over time leading to VVPSS (a), time-evolution of pressure of VV (b), MFR across the RVs of VVPSS (c) and accumulated coolant in the collection tank and VV(d).

The total vapour generated within the plasma chamber passes through the opened valves, as illustrated in figure 4.9c, and is directed towards the HXs, where condensation occurs. Subsequently, the fluid accumulates in the collection tank, as indicated by the red line in figure 4.9d. It is noteworthy that the low mass flow rates discharged via the RVs are intrinsically linked to the accumulation of coolant released from the DEG into the VV, particularly within the lower port where the LOCA event takes place. This behaviour is clearly illustrated by the blue line in figure 4.9d, which refers to the accumulated mass within the VV domain and stabilises at approximately 100 ton towards the end of the simulation.

From the perspective of the PHTS, it can be observed, from the figure 4.10a, that the rapid pressure drop of the loop.

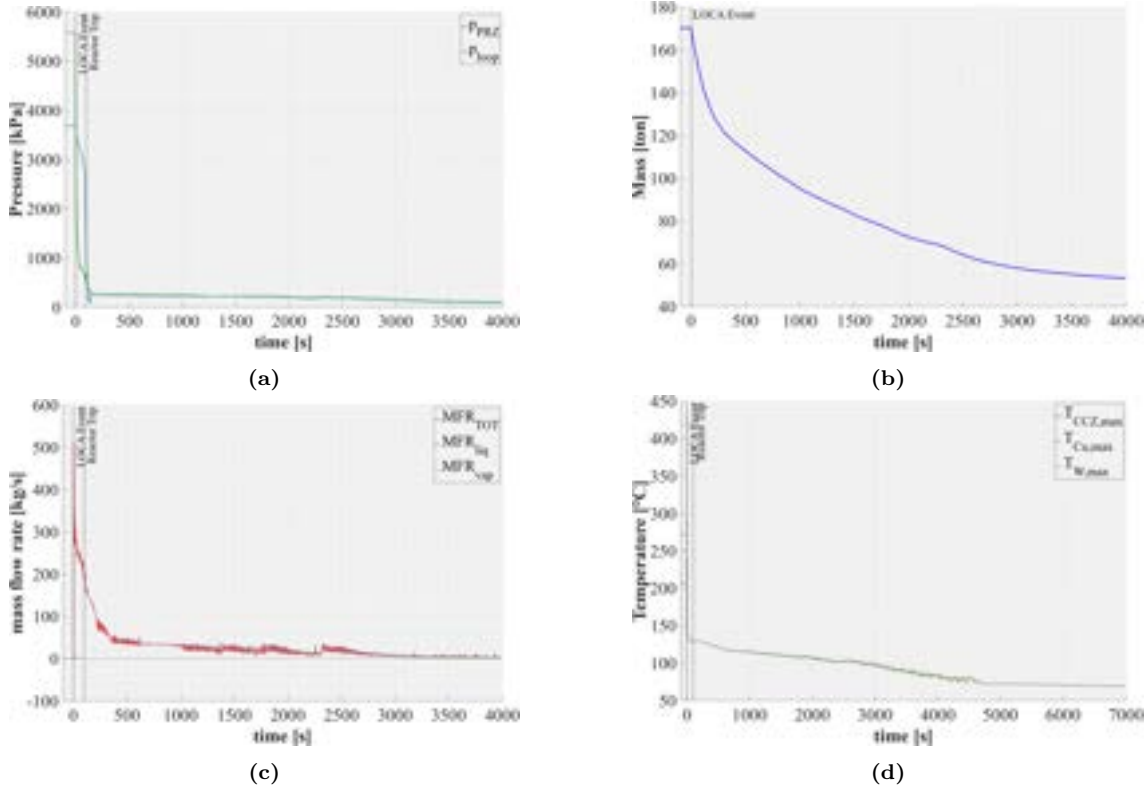


**Figure 4.10:** *PHTS fundamental parameters during in-vessel LOCA: loop pressures time-evolution (a), time-evolution of mass trend in the loop (b), MFR across the DEG rupture (c) and time-evolution of temperature of the PFU unit (d).*

A smoother pressure drop is observed in the early stage of the transient at the PRZ, likely due to the time required for its emptying. It is possible to observe a minimum peak immediately following the complete emptying of the PRZ, associated with a rapid depressurisation of the volume due to the steam condensation, which is connected to the inflow of liquid from the surge line.

Although the coolant inventory decreases as reported in figure 4.10b, the loop mass trend reaches a stable value of 55 ton. This equilibrium can be attributed to the complex geometry of the loop, which spans two vertical levels. In the absence of any active driving force, the primary fluid cannot overcome the pressure losses required to flow from one half-loop to the other. Each half-loop spans both levels, and without sufficient pressure gradient or external forcing, the fluid remains trapped, preventing complete drainage.

Coherently, the Mass Flow Rate (MFR) crossing the DEG is predominantly in the liquid phase, and the most significant coolant release occurs during the initial stage of the transient, as shown in figure 4.10c. The liquid flow rate through the rupture subsequently decreases and stabilises near zero after approximately 2000 s.

Finally, taking into account the temperature of the equivalent PFU unit which reproduces the region of the SP, it is possible to observe a rapid temperature drop associated with the sudden plasma shutdown.

A difference between the pressure in the VV and PHTS regions is visible in figure 4.11. This variation could be influenced by multiple factors, including the layout of the primary circuit and the relative positioning of the components where the pressure is measured, which may contribute to the observed distribution.

From the same figure 4.11, it is also possible to observe the pressure equalisation mechanism within the loop, whereby the pressure at a generic point in the loop rises to match that of the PRZ. Simultaneously, the coolant content exhibits a change in slope (figure 4.10b), accompanied by a slight increase in the release rate through the rupture (figure 4.10c).

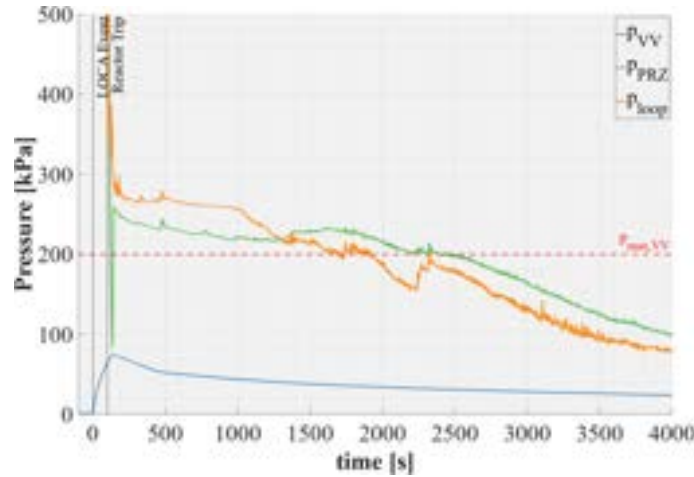


**Figure 4.11:** Pressure trends of VV and PHTS.

Generally, the analysis conducted demonstrates that an in-vessel LOCA, resulting from the DEG rupture of a single PFC cooling circuit, is effectively managed by the VVPSS safety system. This ensures that the pressure within the VV remains well below the prescribed maximum limits.

It is important to note that, as of today, the thermal structures of the DIV are not thermally coupled to the thermal-hydraulic volume of the plasma chamber, as has been done for the FW and SW. However, it is reasonable to assume that there are no significant discrepancies compared to the results presented here in terms of well sizing of the safety system. In any case, should the VV experience higher pressures, additional RVs and BDs can be adopted since they are set to a higher pressure setpoint. Indeed, it is worth recalling that the VVPSS is sized for accidental scenarios related to the BB PHTS, which are considerably more severe due to the coolant masses and the thermodynamic conditions of the cooling circuit.

Based on the results obtained and the design criteria adopted for the sizing of the VVPSS, it has been considered reasonable not to investigate additional in-vessel LOCA in other locations or to multiple cooling circuit failures. This assumption is supported by two main considerations.

First, the position of the LOCA could influence the PHTS behaviour, but as long

as the rupture involves only a single cassette, the response of the VVPSS is expected to remain consistent with the results already presented.

Second, even in the hypothetical case of simultaneous ruptures in multiple in-vessel cooling circuits, the total flow rates involved would remain limited when compared to the BB inventory for which the VVPSS is designed. The transient would still be manageable, thanks to the presence of additional RVs and BDs capable of handling higher pressure conditions.

## 4.3 Ex-vessel LOCA transients

A further accidental scenario investigated for the DIV PFC PHTS, in accordance with section 1.5, is the ex-vessel LOCA. This type of event is characterised by a rupture occurring in any section of the cooling circuit located outside the VV, potentially leading to a complex chain of consequences. These include coolant discharge into the TCR, possible release of tritium and activated products of water and corrosion, overheating of the DIV structures, and ultimately, the risk of structural collapse and in-vessel coolant ingress.

In this section, two representative ex-vessel LOCA transients, referred to as Transient A and Transient B, are analysed to assess the PHTS response under such severe conditions. The scenarios differ in the location of the DEG rupture within the cold leg, which significantly influences the thermal-hydraulic behaviour and the interaction with the PRZ inventory.

The objective is to evaluate the robustness of the PHTS and the structural integrity of the PFC cooling circuit, with particular attention to the time required to reach critical temperature thresholds in the case of CuCrZr material.

### 4.3.1 Ex-vessel LOCA transient A and results

In this paragraph, the consequences of a DEG rupture of the cold leg far from the surge line have been investigated, as shown in figure 4.12.

The DEG rupture is supposed to happen during the pulse phase of the duty cycle figure 1.3 according to the severe power breakdown reported in figure 1.7b. Simultaneously with the event, all non-safety-related control systems are deactivated. In the following case, the reactor trip triggered by the low-pressure signal and the pressure differential signals across the loop (see figure 4.7) is capable of isolating both HXs, activating the pump coast-down and the plasma shutdown.

Specifically, a sensitivity analysis on the plasma shutdown has been conducted, varying the ramp-down duration as reported in the table 4.2.

The CuCrZr temperature of the equivalent PFU unit will be taken into account in order to guarantee the structural requirement reported in section 1.3.4.

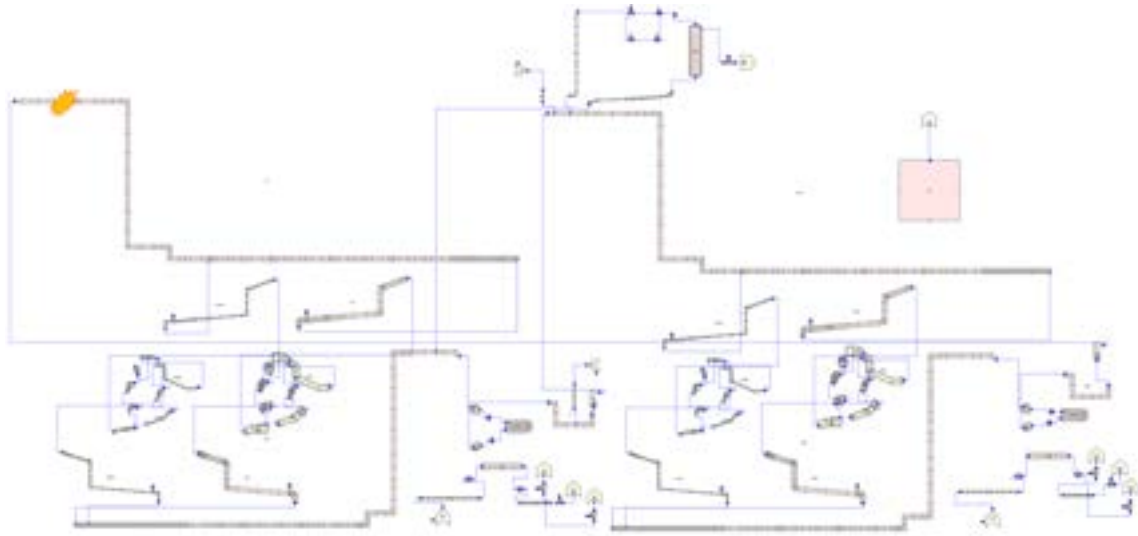


**Figure 4.12:** Cold leg DEG break location.

**Table 4.2:** Investigated ramp down durations.

| Case           | [s] |
|----------------|-----|
| <b>Ramp 01</b> | 50  |
| <b>Ramp 02</b> | 100 |
| <b>Ramp 03</b> | 150 |
| <b>Ramp 04</b> | 200 |

The simulation includes 1500 s of steady-state conditions related to the pulse phase. At 1501 s, the DEG event occurs and the coolant from the cold leg is released in the TCR, where it vaporises. It is considered a transient duration of 3000 s after the accidental event to allow for a comprehensive assessment of the TH variables of the overall system.

As a first result, although the event has been detected promptly in 0.15 s and no delay on the signal has been set, as shown in figure 4.13, it has been observed that none of the selected ramp scenarios have been capable of effectively managing such a severe transient.

All simulations have been interrupted in less than 10 s starting from the coolant release on the building. This outcome reveals a fundamental limitation of the PHTS to survive in such kind of event. In all cases, the failure of the CuCrZr indicates that the system lacks the robustness required to withstand this class of events, and a possible subsequent in-vessel LOCA can occur as a consequence of the ex-vessel initiating event.

Starting from the LOCA detection, in all cases, the first signals that are triggered simultaneously and activate the reactor trip, are those located in the half loop interested by the ex-vessel LOCA loop portion, namely the  $Dp_{01,04}$  followed by  $Dp_{03}$ .

As follows, although the results presented are partial due to the occurrence of component failure, the analysis of the main parameters for all cases will still be

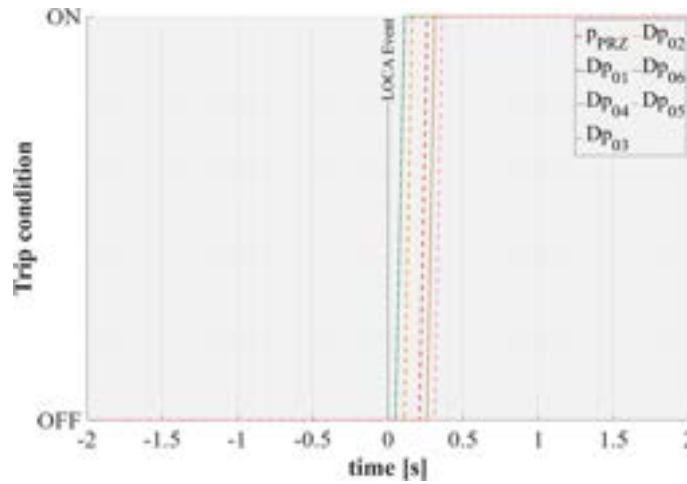


**Figure 4.13:** *Ex-vessel LOCA transient A: reactor trip initiation.*

shown. The aim is to provide insight into the behaviour of the PHTS under such extreme conditions and to extract useful lessons that may inform the development of possible mitigation strategies. From figure 4.14, it is possible to observe that the limit temperature on CuCrZr is exceeded in the collapsed circuit, specifically in the half-loop affected by the LOCA event.

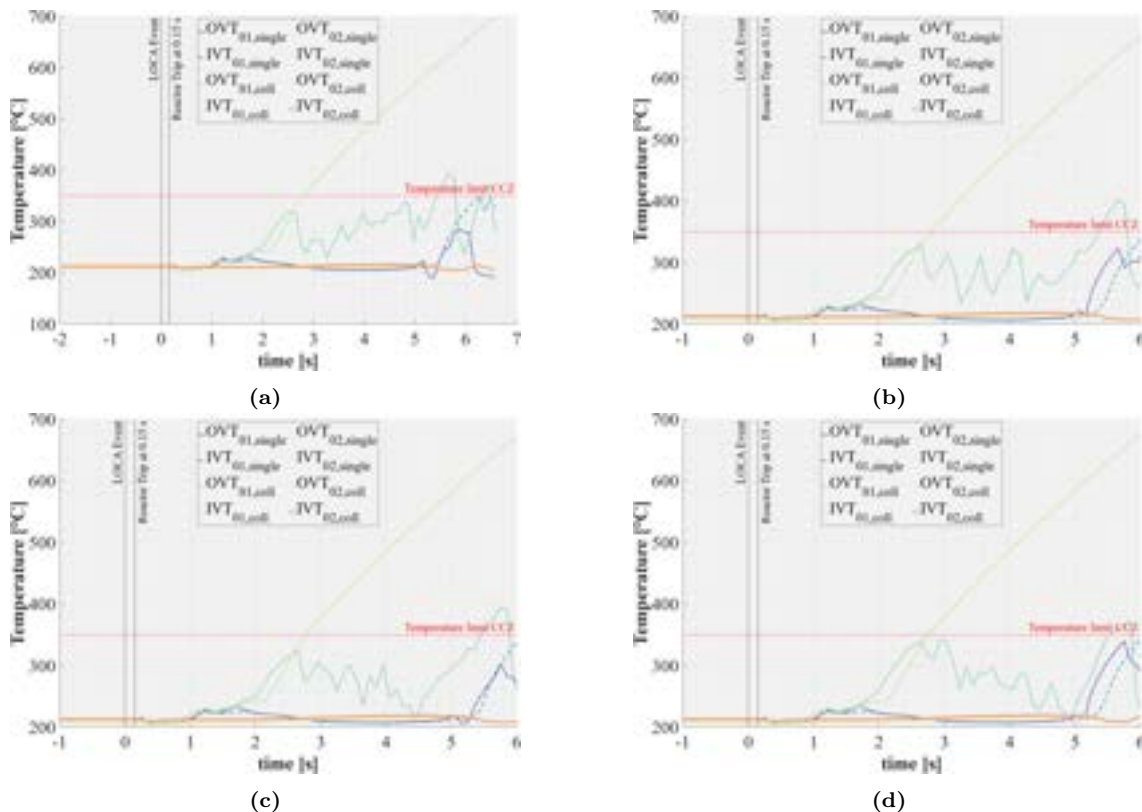


**Figure 4.14:** Time evolution of CCZ maximum temperature during ex-vessel LOCA considering a plasma shutdown in: 50 (a), 100 (b), 150 (c) and 200 seconds (d).

It can be observed that the IVT is the portion experiencing irreversible temperature increases, while the OVT side remains below the limit value. This

behaviour can be attributed to a rewetting process of the thermal structure, which persists during the initial moments following the event.

Since the failure event of the DIV occurs too rapidly to be influenced by the ramp profiles, and although all ramp scenarios have been thoroughly simulated and analysed, the following section focuses on the fastest ramp case. This choice is made for conciseness, as the trends observed across the different ramps are analogous.

As illustrated in figure 4.15a, the pressure within the loop, upstream of the leakage, undergoes a rapid and significant decrease following the onset of the transient.

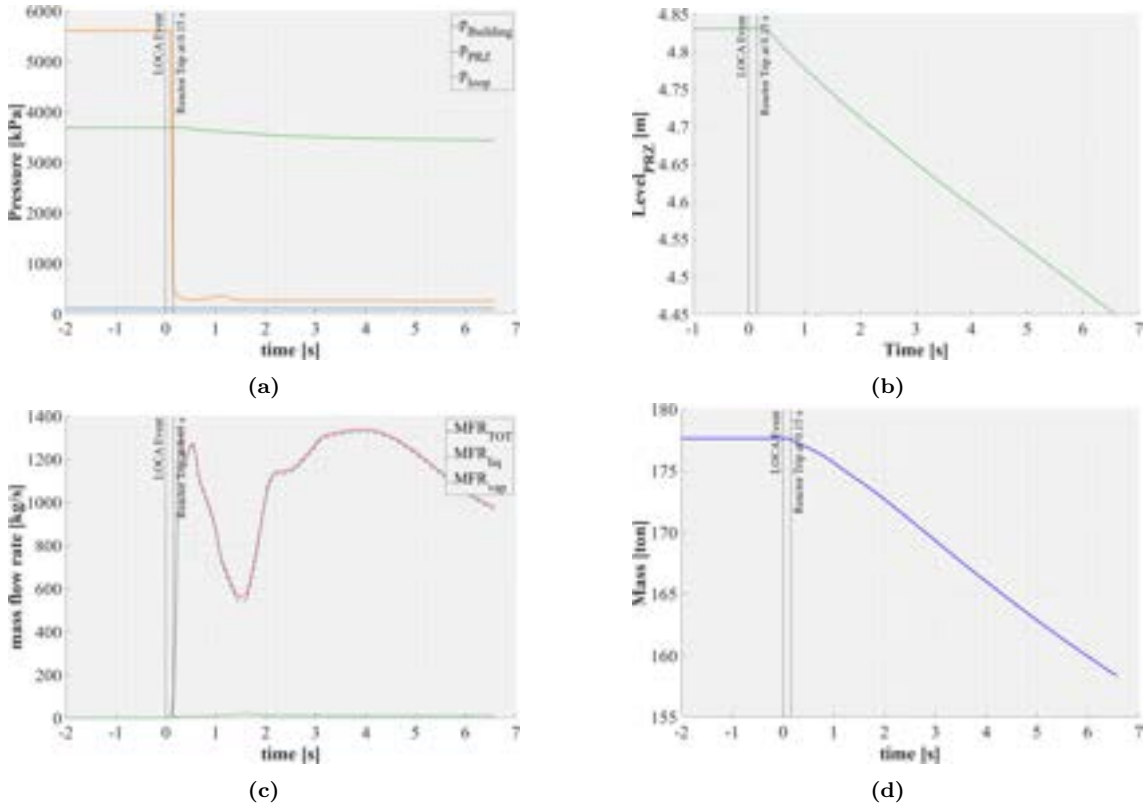


**Figure 4.15:** PHTS fundamental parameters during ex-vessel LOCA transient A: pressures comparison between loop and TCR (a), time-evolution of PRZ water level (b), MFR across the DEG rupture (c) and time-evolution of mass trend in the loop (d).

In contrast, the pressure in the PRZ remains relatively stable during the initial phase, primarily due to the presence of a sufficient water inventory within the component, which acts as a buffer.

Due to the higher dimension of the volume that simulates the building, its pressure is not affected by the release of coolant. The water level inside the PRZ decreases by only 7 % compared to the reference value, as shown in figure 4.15b. This limited reduction suggests that the pressurizer retains a substantial portion of its initial content, which contributes to maintaining the pressure despite the perturbations occurring in the primary loop.

However, at this stage of the transient, a faster emptying of the PRZ would be

beneficial, potentially improving the thermal response of the half-loop affected by the accidental event. This would require a revision of the flow area of the surge line, to enable a quicker flow of the coolant from the PRZ to the loop and enhance the performance of the system in case of such a severe event.

The MFR across the DEG is reported in figure 4.15c while the current mass in the loop is shown in figure 4.15d.

Generally, it can be observed that the failure of the in-vessel PFC cooling circuit occurs too rapidly to allow for any significant changes to be detected in the other loop parameters except for the pressure of the loop. Moreover, it is crucial to highlight the speed at which the failure unfolds in the collapsed model of the in-vessel cooling circuits, clearly demonstrating the impossibility of managing such a severe accident through any ramp duration strategy.

Based on the results, even though it is not explicitly modelled, it can be reasonably confirmed, under the assumption of the event, that an ex-vessel LOCA constitutes a severe accident that can subsequently trigger an in-vessel LOCA.

### 4.3.2 Ex-vessel LOCA transient B and results

Based on the results of the previous ex-vessel LOCA, in order to investigate a more severe event and observe the failure time that depends on the PRZ behaviour, the consequences of a further DEG rupture have been investigated as shown in figure 4.16.

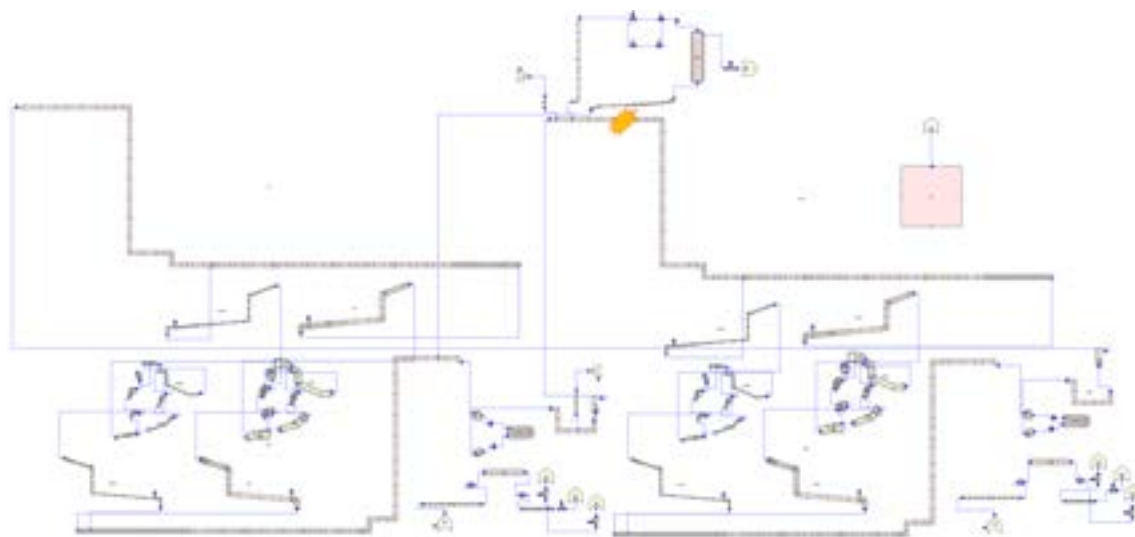


**Figure 4.16:** *Cold leg DEG break location severe condition.*

In particular, the DEG has been located downstream of the surge line so that the first cold half loop does not feed from the stored mass in the PRZ.

In analogy with the previous case section 4.3.1, the DEG rupture is supposed to happen during the pulse phase of the duty cycle. The same assumptions regarding

the transient evolution, namely the reactor trip and the hypothesised simulation times, have been adopted (see section 4.3.1 for further details).

Given that it has already been observed that the transient behaviour, particularly the failure of the PFCs, has occurred very rapidly, it has been concluded that a sensitivity analysis on the plasma shutdown ramps would not provide any meaningful insights. For this reason, the subsequent transient scenario has been considered only with a faster shutdown ramp of 50 s.

As already experienced for the Transient A, although the detection signals have been abrupt, the simulation has been interrupted by the in-vessel component failure within the first seven seconds of the accident transient. As a result, the activation of the isolation and plasma shutdown systems has not provided any effective support.

As shown in figure 4.17, the first signals that are triggered simultaneously and activate the reactor trip are those located in the half loop interested by the ex-vessel LOCA loop portion, namely the  $Dp_{02,06}$  followed by  $Dp_{05}$ .

Concerning the maximum temperature reached in the CuCrZr, as shown in figure 4.18, it can be observed that all PFCs within the half-loop subjected to LOCA exceed the temperature limit of the CuCrZr. In particular, the single cassette reaches this threshold within 2.7 s, starting from the OVT region.

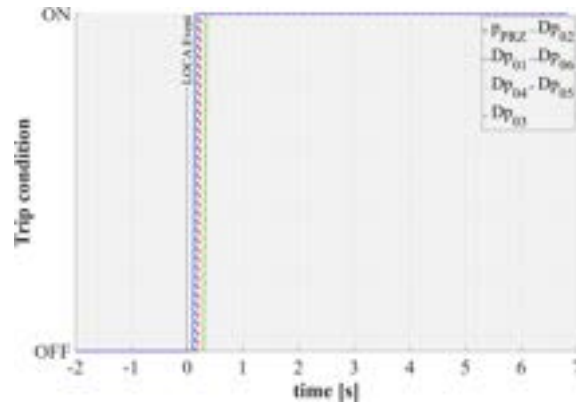


**Figure 4.17:** *Ex-vessel LOCA transient B: reactor trip initiation.*

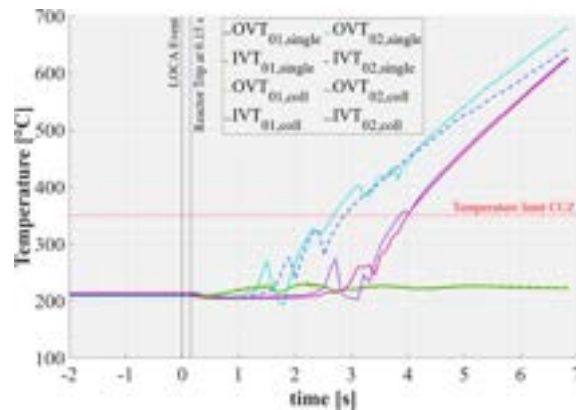


**Figure 4.18:** *Ex-vessel LOCA transient B: Time evolution of CCZ maximum temperature.*

It is worth noting that the thermal behaviour of the VTs in both individual and collapsed cassettes appears uniform, in contrast to the first ex-vessel LOCA scenario. This difference may be attributed to the adopted modelling strategy and the inherent complexity of the system.

As expected, the time required to reach the material temperature limit has been anticipated, although it does not result in significant deviations from the behaviour observed in the initial ex-vessel LOCA case.

This analysis highlights how the plasma operation has emerged as the driving factor behind the failure of the DIV, underscoring the inability of the PHTS to effectively manage this type of transient. In any case, and in analogy with what has been discussed in section 1.5, the system experiencing this event is expected to undergo an in-vessel LOCA.

For completeness and in analogy with the previous analysis, the main parameters of the circuit are reported in figure 4.19, as has been done in section 4.3.1.

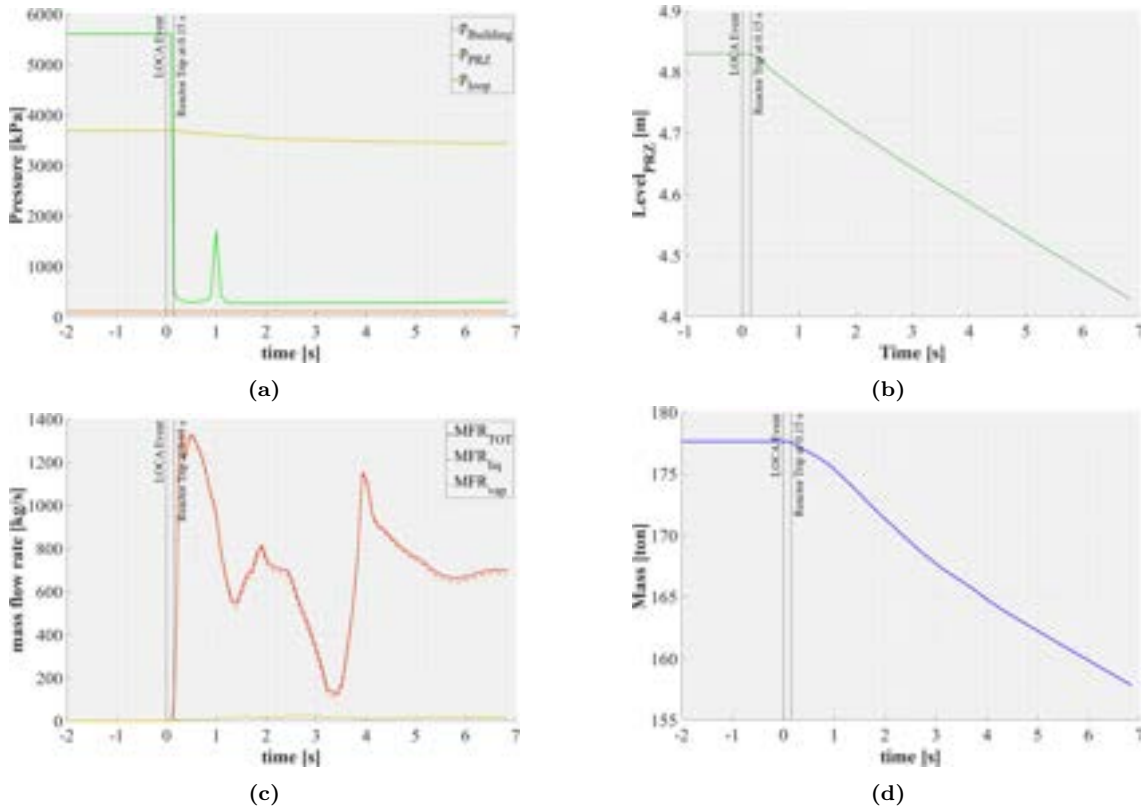


**Figure 4.19:** PHTS fundamental parameters during ex-vessel LOCA transient B: pressures comparison between loop and TCR (a), time-evolution of PRZ water level (b), MFR across the DEG rupture (c) and time-evolution of mass trend in the loop (d).

All parameters, with some differences, appear to be largely consistent with those observed in case A. The pressure peak of the loop, shown in figure 4.19a, measured upstream of the rupture, can be attributed to the presence of vapour generated by the flow of coolant from the hot leg and surge line. This coolant is no longer able to exchange with the mass flow rate of the secondary side, as the HX has been isolated.

The PRZ level decreases slightly more, as do the coolant masses in the circuit (figures 4.19b and 4.19d). The flow rates through the rupture (figure 4.19c) exhibit a clearly distinct behaviour, which can be directly attributed to the specific and localised position of the DEG within the system.

### 4.3.3 Ex-vessel LOCA outcomes

The analysis of the two ex-vessel LOCA transients has provided valuable insights into the behaviour of the DIV PFC PHTS, although the model includes simplifications. In particular, the following simplifications have been made:

- the TRACE model adopts a collapsed representation of the cooling loops, where only one cassette per half-loop is modelled in detail, and the remaining circuits are merged into equivalent pipework;
- the TRACE model is strictly focused on TH behaviour excluding the radiological inventories such as tritium;
- the VV pressure has been set to the lowest allowable value to avoid numerical instability, without affecting the transient evolution;
- no delay time has been set for triggering the reactor trip;
- the thermal structures of the DIV are not coupled to plasma chamber;

Regarding the first point, while the collapsed loop approach is computationally efficient, it may not fully capture localised effects and flow asymmetries that could arise in a real configuration, especially during fast transients. Nevertheless, the modelling choices adopted do not compromise the validity of the results and have clearly demonstrated the vulnerability of the cooling system.

Despite the differences in DEG rupture location, both scenarios have demonstrated the intrinsic vulnerability of the PHTS when subjected to this kind of severe event.

In both cases, the failure of the in-vessel cooling components occurs within the first few seconds following the DEG rupture, well before any mitigation strategy, such as the reactor trip, can exert a meaningful effect.

As a result, the thermal limit of the CuCrZr is exceeded rapidly, leading to irreversible damage and potential structural collapse of the DIV.

Importantly, the progression of the accident in both transients suggests a high likelihood of a secondary in-vessel LOCA, caused by the failure of the in-vessel cooling circuit and subsequent ingress of coolant into the VV. In such cases, the previously analysed VVPSS safety system becomes relevant. As demonstrated in section 4.2, the VVPSS is capable of effectively managing the pressure rise within the VV, ensuring containment and limiting the consequences of the event.

Given this, it becomes natural to consider the application of plasma control

strategies aimed at reducing the thermal load on the DIV during such transients to shutdown the machine, hopefully safely.

Among these, the sweeping mitigation strategy, which is the main focus of chapter 3, results of interest since it seems to be applicable also in normal operating conditions of plasma when the DIV operates in detachment mode [66, 67].

Although a detailed calculation goes beyond the scope of the present dissertation, it is reasonable to assume a sweeping of 200 mm and 1 Hz of the heat flux profile on the DIV in detached condition section 3.3.4. A more accurate estimation would require an additional modelling step involving the coupling PHTS with a Python script to reproduce the sweeping in normal operating conditions of plasma via the ECI interface. However, this coupling has so far been tested only on a single in-vessel cooling circuit of PFCs, and its extension to a full-sector configuration would require further development and validation efforts. While such an approach is feasible in principle, it is not readily implementable within the current framework due to the complexity of the coupling process.

In this regard, figure 4.20a illustrates the heat flux distribution applied to the TRACE model for the OVT.

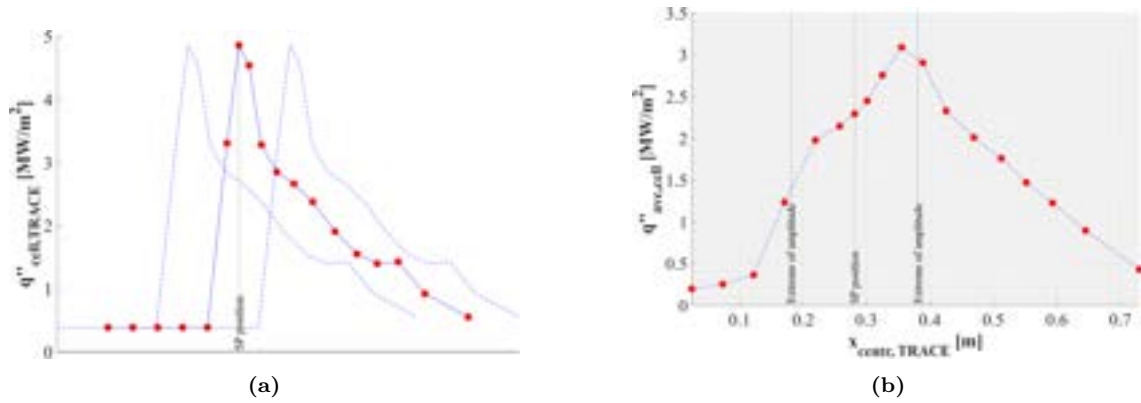


**Figure 4.20:** Swept heat flux profile in detachment condition: (a) schematic drawing, (b) axial distribution of the average heat flux over time.

The continuous line represents the reference profile without sweeping, while the red dots correspond to the discrete heat flux values imposed on the thermal cells. The dashed lines show the same profile shifted to the extremes of the sweeping amplitude.

Therefore, given the imposed sweeping conditions, it is possible to derive the time-averaged heat flux profile along the entire OVT, as shown in the figure 4.20b. Once again, the trend is represented by the blue dashed line, while the red dots indicate the discrete values that the thermal cells of the TRACE model would experience on average. Similar to what has been observed in chapter 3, the most critical region in terms of the highest time-averaged heat flux is located near the extreme of the sweeping amplitude, namely the area that is already subjected to higher flux under normal operating conditions.

As shown in the figure 4.20b, the maximum average heat flux is around  $3.1 \text{ MW/m}^2$ , which is lower than the  $5 \text{ MW/m}^2$  value at the SP without sweeping. This reduction could help delay the onset of critical temperatures in the most exposed regions of the DIV during an ex-vessel LOCA scenario. It is worth highlighting that, differently from the loss of detachment condition, the percentage reduction of the peak heat flux is around 38 %, which suggests that the effects of sweeping under normal operational conditions are not particularly advantageous.

In summary, while the current results do not support the effectiveness of sweeping in the specific ex-vessel LOCA scenarios investigated, the methodology developed in chapter 3 remains applicable and could be extended to these severe transients.

# Conclusions

This thesis reflects the research activity carried out during the XXXVIII cycle of the Ph.D. course in Energy – Low-carbon Energetics and Innovative Nuclear Systems – at the University of Palermo. Between 2022 and 2025, the work has been embedded within the conceptual design studies of DEMOnstration fusion power reactor (DEMO), under the EUROfusion framework.

The research has focused on the Thermal-Hydraulic (TH) behaviour of the cooling circuit of the Divertor (DIV) Plasma Facing Components (PFCs) and its Primary Heat Transport System (PHTS) as a whole, with particular attention to up-set and accidental conditions.

The work has developed along two main and complementary directions:

- the development of an integrated and multidisciplinary modelling approach for the simulation of complex transients, such as Strike Point (SP) sweeping during loss of detachment for the PFCs single in vessel cooling circuit. This required overcoming the limitations of conventional System Codes, which are not inherently suited to capture the spatially and temporally variable thermal loads typical of fusion environments;
- the application of the developed methodology to the analysis of accidental scenarios, in particular in-vessel and ex-vessel Loss of Coolant Accident (LOCA), with the aim of assessing the PHTS and the related safety system ability to manage such events safely.

To support both directions, a robust, flexible and multidisciplinary simulation framework has been developed, combining:

- the capability of TRAC/RELAP Advanced Computational Engine (TRACE) to simulate large-scale TH behaviour;
- the spatial and temporal resolution of ANSYS-CFX for detailed local thermal analyses;
- the development of optimisation algorithms and regression models to reproduce the dynamics of complex 3D geometries within simplified models;
- the flexibility of Python scripting to define dynamic boundary conditions that account for plasma evolution and its effects on in-vessel components;
- the coupling capabilities of the Exterior Communications Interface to synchronise external tools with TRACE in real-time.

This approach enabled the accurate simulation of the TH response of the Vertical Targets (VTs) under sweeping conditions, and the assessment of mitigation strategies. At the same time, it allowed for the refinement of the system model to perform LOCA analyses, providing insights into the PHTS behaviour under severe accident conditions and supporting the assessment of safety-related systems.

A particular focus has been placed on the modelling of the Monoblock (MB) structure. Recognising the limitations of TRACE in representing 3D geometries and dynamic plasma-wall interactions, an optimised equivalent thermal structure has been introduced. This multi-layer cylindrical model has been tuned to replicate the energy content and temperature evolution of the real Plasma Facing Unit (PFU). Validation against literature references and detailed ANSYS-CFX simulations confirmed its accuracy, and further refinements (based on analytical methods and regression modelling) allowed for a balance between computational efficiency and physical fidelity.

Starting from this core work, it has been possible to study the PFCs in-vessel cooling circuit under sweeping conditions in case of loss of detachment. The study has been enriched by the development of various Python scripts coupled to TRACE simulation to reproduce the Eich profile, plasma current and power shutdown.

The results confirm the effectiveness of the sweeping strategy while also revealing distinct thermal challenges across the VTs. In particular, the regions located at the extremes of the sweeping amplitude (where the SP reverses direction) emerge as the most vulnerable, even more so than the region initially impacted by the Eich peak. In one of these regions of the Outer Vertical Target (OVT), taken as a reference, the Copper-Chromium-Zirconium (CuCrZr) has been observed to exceed the temperature limit of 350 °C, while the Tungsten (W) has reached the recrystallisation threshold, despite the implementation of a plasma shutdown. These findings are essential for guiding future design enhancements and refining operational strategies.

Concerning the second line of research treated, the developed methodology has been extended to the analysis of accidental scenarios, focusing on both in-vessel and ex-vessel LOCA, which are critical events for the DIV PFC PHTS.

The in-vessel LOCA, triggered by a Double-Ended Guillotine rupture in a single PFC in-vessel cooling circuit, has demonstrated that the Vacuum Vessel Pressure Suppression System (VVPSS) is capable of effectively managing the transient, maintaining the Vacuum Vessel (VV) pressure below the design threshold and ensuring containment of the released coolant. These results are consistent with expectations, considering that the VVPSS has been sized to handle more severe transients associated with Breeding Blanket PHTS.

On the other hand, the ex-vessel LOCA analyses have revealed significant vulnerabilities in the current system configuration. The rapid thermal degradation

of the DIV structures, particularly the CuCrZr alloy, occurs within seconds of the rupture, even under fast plasma shutdown conditions. These findings highlight the intrinsic limitations of the PHTS in coping with severe ex-vessel transients and suggest a high likelihood of secondary in-vessel LOCA events.

Based on the results of the ex-vessel LOCA and the integrated modelling approach to simulate mitigation strategies, it seems natural to consider the application of sweeping even in normal operation conditions of the plasma, with the hope of delaying the onset of critical temperatures and avoiding in-vessel LOCA. Although its effectiveness and applicability in such scenarios remain to be demonstrated, the modelling tools developed in this work could support a dedicated investigation of this possibility.

While this aspect has not been directly addressed in the dissertation, some preliminary considerations have been made, suggesting a potential reduction in the average heat flux on the OVT. However, from these embryonic observations, no significant reduction has been observed in time-average heat flux across the OVT, unlike the case of loss of detachment.

Overall, the methodology developed and treated in the present dissertation has proven to be a versatile and effective tool for supporting both the design and the safety assessment of the DEMO DIV. Its modular structure and the ability to couple the tools make it highly adaptable to a wide range of operating scenarios and thermal load profiles. This flexibility not only enables detailed analyses of specific configurations but also opens the door to parametric investigations and future optimisation studies.

Looking ahead, several promising directions for further development can be identified.

One natural extension involves the application of the proposed methodology to other in-vessel components. In this context, the advantages of faster TH simulations, based on simplified but effective modelling approaches, might be exploited to reproduce the complex thermal interactions between in-vessel components and the plasma during transient conditions. Such an extension would allow for broader system-level analyses while maintaining computational efficiency, and could support the design and optimisation of additional critical systems.

Another potential direction is the integration of structural damage and thermal fatigue simplified models, which would provide a more comprehensive understanding of the DIV long-term behaviour under cyclic and extreme operating conditions.

Finally, although the methodology has been developed within the framework of the DEMO DIV, its modular and flexible nature suggests that, in principle, it could be adapted to other fusion reactor configurations. Such adaptation would require revisiting boundary conditions, plasma interaction models, and geometric assumptions to reflect the specific characteristics of alternative concepts.

Nevertheless, the core approach, which combines system-level TH simulations with external scripting to reproduce the thermal loads related to the plasma behaviour as boundary conditions on the in-vessel component, offers a promising foundation for broader applications in fusion technology.

In conclusion, the work carried out has contributed to the understanding and modelling of critical TH phenomena in the DEMO DIV, providing a technical basis and a set of modelling tools that may support ongoing efforts aimed at developing the conceptual design of DEMO.

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