



**Università
degli Studi
di Palermo**

AREA RICERCA E TRASFERIMENTO TECNOLOGICO
SETTORE DOTTORATI E CONTRATTI PER LA RICERCA
U. O. DOTTORATI DI RICERCA

Corso di Dottorato di Ricerca in
Energy – Low Carbon Energetics and Innovative Nuclear Systems
Dipartimento di Ingegneria
Settore Scientifico Disciplinare ING-IND/19

MULTIPHYSICS OPTIMIZATION OF THE DEMO HELIUM- COOLED PEBBLE BED MONOBLOCK BREEDING BLANKET

IL DOTTORE
ALBERTO GIOÈ

LA COORDINATRICE
**CH.MA PROF.SSA E. RIVA
SANSEVERINO**

I TUTOR
**CH.MO PROF. P. A. DI MAIO
CH.MO PROF. P. CHIOVARO**

CICLO XXXVIII
ANNO CONSEGUIMENTO TITOLO 2025

Contents

List of figures	IV
List of tables.....	VIII
List of acronyms.....	X
Abstract	XII
Introduction	1
Chapter 1 The HCPB monoblock Breeding Blanket of the DEMO Fusion Reactor ..	5
1.1 Elements of fusion reaction physics	5
1.2 The DEMO fusion reactor	12
1.3 The HCPB Breeding Blanket: design evolution, status and limitations	15
1.4 The HCPB monoblock Breeding Blanket.....	18
1.4.1 Baseline HCPB monoblock pin geometry	19
Chapter 2 Constraints, objectives, and parameters of the HCPB monoblock pin layout optimization 21	
2.1 Introduction.....	21
2.2 Design constraints and objectives for HCPB monoblock BB	21
2.2.1 Structural and functional materials	21
2.2.2 Coolant.....	23
2.3 Investigated parameters and geometric features	24
2.3.1 Thickness of the outlet channel t_1	25
2.3.2 Jet impingement shape.....	27
2.3.3 Double and single helium flow pass	28
2.3.4 Radius of the inlet tube r_1	28
2.3.5 Thickness of the outer annular channel t_2	30
2.3.6 Roughness of coolant-wetted surfaces.....	30
2.4 Examined working conditions	32

2.4.1	Coolant temperature rise ΔT	32
2.4.2	First wall Heat Flux	32
Chapter 3	Thermofluid-dynamic analysis of a pin of the HCPB monoblock BB	34
3.1	Introduction.....	34
3.2	Description of the methodology	34
3.3	2D CFD model setup	40
3.3.1	Calculation grid.....	40
3.3.2	Material properties	42
3.3.3	Heat loads	44
3.3.4	Boundary conditions	47
3.4	Grid independence evaluation	48
3.5	Thermofluid-dynamic analysis campaign.....	53
3.5.1	Baseline HCPB monoblock pin analysis campaign.....	55
3.5.2	Comparison between case 2 and case 3 layouts	59
3.5.3	Parametric analysis on outlet channel thickness t_1	62
3.5.4	In-depth analysis on the case 2 layout	66
3.5.5	Investigation on an alternative jet impingement shape.....	68
3.5.6	Analysis on a pin layout with a single flow pass.....	71
3.5.7	Parametric analysis on inlet tube radius r_1	75
3.5.8	Investigation on outer annular channel thickness t_2	80
3.5.9	Final remarks on the thermofluid-dynamic analysis campaign	85
3.6	Conclusions of the thermofluid-dynamic analysis.....	89
Chapter 4	Thermomechanical analysis of analysis of a pin of the HCPB monoblock BB	92
4.1	Introduction.....	92
4.2	Description of the methodology	92
4.3	Global FEM analysis	102
4.3.1	Global FEM analysis setup	102

4.3.2	Global FEM analysis results	108
4.4	Submodel analysis	112
4.4.1	Submodel analysis setup	112
4.4.2	Submodel analysis results	117
4.5	Grid independence assessment	125
4.6	Conclusions of the thermomechanical analysis	129
	Conclusions	132
	References	135
	Appendix A	141
	Appendix B	151

List of figures

Figure 1.1: binding energy per nucleon as a function of atomic mass number [22].	5
Figure 1.2: Depiction of electrostatic repulsive potential ($r > R_0$) and nuclear attraction ($r < R_0$) [23].	6
Figure 1.3 (a,b,c): Depiction of the three magnetic field topologies and illustrative ion .	9
Figure 1.4: Conceptual representation of a tokamak (left) and stellarator (right) device.	9
Figure 1.5: Simplified scheme of a tokamak fusion device [26].	10
Figure 1.6: European Roadmap [4].	11
Figure 1.7: Representation of the main components of DEMO [29].	14
Figure 1.8: Three-dimensional layout of the HCPB BB PHTS [31].	14
Figure 1.9: DEMO BL2017 configuration: (a) IsoView of DEMO BL2017; (b) top view of DEMO BL2017 and blanket sector [11].	15
Figure 1.10: Design evolution of HCPB BB during PCD phase [11].	16
Figure 1.11: Fuel-breeder pin concept of the HCPB BB in 2018 [11].	17
Figure 1.12: Detailed view of a fuel-breeder pin with inverted funnel shape [11].	17
Figure 1.13: HCPB BB at the end of the PCD phase [11].	18
Figure 1.14: 3D representation of the steel domains of the innovative pin monoblock architecture [47].	19
Figure 1.15: 2D CAD model of the baseline HCPB monoblock pin layout (symmetric about the X-axis). Blue arrows illustrate coolant flow.	20
Figure 2.1: Investigated geometric features in double coolant flow pass layouts.	25
Figure 2.2: Investigated geometric features in single coolant flow pass layouts.	25
Figure 2.3: Outlet channel thickness t_1 .	26
Figure 2.4: Apical area of baseline (case 1) geometry (middle), case 2 and case 3 (sides).	26
Figure 2.5: Helium domains of the case 1 (baseline) pin layout, with detailed view on the apical area.	27
Figure 2.6: Case 4 geometry and comparison with case 2 layout.	27
Figure 2.7: Case 5 geometry and comparison with case 1 layout.	28
Figure 2.8: Inlet tube radius r_1 , shown in baseline pin geometry.	28
Figure 2.9: Inlet tube radius r_1 , shown in case 5 pin geometry.	29
Figure 2.10: Outer annular channel thickness t_2 , shown in baseline pin geometry.	30

Figure 2.11: Outer annular channel thickness t_2 , shown in case 5 layout.....	30
Figure 2.12: Illustration of the surfaces where feasibility of machining has been assumed (pin layouts with double helium flow pass).....	31
Figure 2.13: Illustration of the coolant-wetted surfaces of single flow pass layouts.....	31
Figure 3.1: Transition from a coarser mesh in the core flow region to a more refined mesh at solid-fluid interface.	38
Figure 3.2: K- ω SST model switches between k- ϵ and k- ω turbulence model using a blending function [65].	39
Figure 3.3: Mesh of the baseline pin layout according to the parameters of Table 3.1.	41
Figure 3.4: Mesh of the case 5 pin layout according to the parameters of Table 3.2.	42
Figure 3.5: Nuclear Heating applied to breeder, multiplier, and Eurofer domains.....	45
Figure 3.6: Sensitivity of the velocity profile in the inlet tube to mesh element size.	50
Figure 3.7: Sensitivity of the velocity profile in the inlet tube to mesh refinement at fluid-solid interfaces.	52
Figure 3.8: Overview of the analysed parameters for double (top) and single (bottom) flow pass pin geometries.....	53
Figure 3.9: Temperature field in solid and fluid domains of case 1 layout.	55
Figure 3.10: Fluid velocity vector plot for case 1 layout: details of vortices and fluid vein detachments.	56
Figure 3.11: Mean values of temperature peaks in the different structures.	56
Figure 3.12: Peak temperatures in Eurofer cap and cladding, and coolant total pressure drop observed in case 1 layout when increasing HF_0 with fixed ΔT and surface roughness.	57
Figure 3.13: Apical area of baseline (case 1) geometry (middle), case 2 and case 3 (sides).	60
Figure 3.14: Comparison between case 2 and case 3 pin layout performance.	61
Figure 3.15: Apical area of case 1 (baseline) geometry (left), and case 8 (right).	62
Figure 3.16: Comparison of the performance of the pin layouts with different t_1	63
Figure 3.17: Maximum Eurofer temperatures as a function of plasma radiation load in high roughness analyses on case 2 layout.....	67
Figure 3.18: Total pressure drop as a function of plasma radiation load in high roughness analyses on case 2 layout.	67
Figure 3.19: Case 4 geometry and comparison with case 2 layout.	68
Figure 3.20: Comparison between case 2 and case 4 pin layout performance.	70

Figure 3.21: Fluid velocity vector plot in jet impingement region of case 4 and case 2 layouts.....	70
Figure 3.22: Case 5 geometry and comparison with case 2 layout.....	72
Figure 3.23: Temperature field in solid and fluid domains of case 5 layout.	73
Figure 3.24: Comparison between case 2 and case 5 pin layout performance.	74
Figure 3.25: Apical area of case 2 geometry (left), and case 12 (right).....	76
Figure 3.26: Comparison of the performance of the pin layouts with different r_1 and a double coolant flow pass.....	77
Figure 3.27: Apical area of case 10 geometry (left), and case 14 (right).....	78
Figure 3.28: Comparison of the performance of the pin layouts with different r_1 and a single coolant flow pass.....	79
Figure 3.29: Illustration of outer channel thickness t_2 in double flow pass layouts.....	80
Figure 3.30: Apical area of case 13 geometry (left), and case 15 (right).....	83
Figure 3.31: Comparison between case 13 and case 15 pin layout performance.	84
Figure 3.32: Comparison of the performance of six promising pin layouts in reference working conditions.	87
Figure 3.33: Comparison of the performance of six promising pin layouts under HF_0 of 0.48 MW/m^2	88
Figure 3.34: Comparison of the performance of six promising pin layouts under HF_0 of 0.65 MW/m^2	89
Figure 4.1: Global model and submodel mesh.....	96
Figure 4.2: Global model and submodels for analysis on a pin of the HCPB monoblock COB segment (submodels shown in section).	97
Figure 4.3: Stress linearization [73].	100
Figure 4.4: Global model analysis - the 3D geometric model of the equatorial region of the COB segment [47].	103
Figure 4.5: Global model analysis - details of the 3D geometric model [47].	103
Figure 4.6: Discretization adopted for global model analysis.....	104
Figure 4.7: 2D thermal fields adopted for the thermomechanical assessment [47].	105
Figure 4.8: 2D thermal field of one pin (case 5 layout) mapped onto the global model as an imported load for the thermal analysis.....	105
Figure 4.9: Ansys Python script that partially automates the mapping of the 2D pin thermal fields to the 3D global model.	106
Figure 4.10: Global model analysis – mechanical restraints [47].	107
Figure 4.11: Global model analysis – 3D thermal fields [47].	108

Figure 4.12: Global model - Case 2 - Nominal pressure - Von Mises equivalent stress [47].....	109
Figure 4.13: Global model - Case 2 - Nominal pressure - Total displacement [47].	109
Figure 4.14: Global model - Case 5 - Nominal pressure - Von Mises equivalent stress [47].....	109
Figure 4.15: Global model - Case 5 - Nominal pressure - Total displacement [47].	110
Figure 4.16: Global model - Case 2 - Design pressure - Von Mises equivalent stress [47].	111
Figure 4.17: Global model - Case 2 - Design pressure - Total displacement [47].....	111
Figure 4.18: Global model - Case 5 - Design pressure - Von Mises equivalent stress [47].	111
Figure 4.19: Global model - Case 5 - Design pressure - Total displacement [47].....	112
Figure 4.20: Discretization adopted for case 2 submodel analysis: full geometry (top) and section view (bottom).	113
Figure 4.21: Discretization adopted for case 5 submodel analysis: full geometry (top) and section view (bottom).	114
Figure 4.22: Pin 3D thermal field - case 2 (view in section) [47].....	115
Figure 4.23: Pin 3D thermal field - case 5 (view in section) [47].....	115
Figure 4.24: Nominal pressure - The imposed displacement field [47].....	116
Figure 4.25: Design pressure - The imposed displacement field [47].	117
Figure 4.26: Submodel - Case 2 - Nominal pressure - Von Mises equivalent stress [47].	118
Figure 4.27: Submodel - Case 5 - Nominal pressure - Von Mises equivalent stress [47].	118
Figure 4.28: Sub model - Case 2 - Design pressure - Von Mises equivalent stress [47].	119
Figure 4.29: Sub model - Case 5 - Design pressure - Von Mises equivalent stress [47].	119
Figure 4.30: Case 2 - Path locations [47].....	120
Figure 4.31: Case 5 - Path locations [47].....	121
Figure 4.32: Case 2 – Design pressure – Metal sheets axial displacement [47].	124
Figure 4.33: Percentage error distribution on normal (left) and shear (right) stress components, for analyses D ₁ to D ₄ (top to bottom).	128
Figure 4.34: Numerical convergence on evaluation of IPFL utilization factors.....	129

List of tables

Table 1.1: Principal nuclear reactions [24].	7
Table 2.1: Operating temperatures for tungsten armour, Eurofer, ACB pebbles, and Beryllide [49].	22
Table 2.2: Helium pressure range in HCPB BB [53] [54].	23
Table 2.3: Cooling helium operating temperatures.	24
Table 2.4: Analysed cases with different t_1 values.	26
Table 2.5: Analysed combinations of parameters t_1 and r_1	29
Table 2.6: Analysed cases with different r_1 values and a single helium flow pass.	29
Table 2.7: Analysed combinations of r_1 and t_2	30
Table 2.8: Roughness values applied on machinable surfaces in parametric analyses.	31
Table 2.9: Roughness applied on machinable surfaces in high roughness analyses.	31
Table 2.10: Coolant temperature rise values examined in parametric analyses.	32
Table 2.11: HF_0 values examined in parametric analyses.	33
Table 3.1: Summary of mesh settings.	40
Table 3.2: Summary of mesh settings for high roughness analyses.	41
Table 3.3: Thermal conductivity of ACB pebbles.	43
Table 3.4: Thermal conductivity of beryllide blocks.	43
Table 3.5: Thermal conductivity of Eurofer.	43
Table 3.6: Summary of the thermofluid-dynamic analyses setup.	47
Table 3.7: Total pressure drop sensitivity to mesh element size.	49
Table 3.8: Fluid velocities at three radial positions in the inlet tube (M_2 analysis).	50
Table 3.9: Percentage differences between evaluated fluid velocities at three radial positions in the inlet tube among the analyses M_1 , M_2 , and M_3	51
Table 3.10: Main mesh features adopted for M_4 , M_2 and M_5 analyses.	51
Table 3.11: Total pressure drop sensitivity to mesh refinement at fluid-solid interfaces.	51
Table 3.12: Percentage differences between evaluated fluid velocities at three radial positions in the inlet tube among the analyses M_4 , M_2 , and M_5	52
Table 3.13: Characteristics of the pin layouts assessed in the thermofluid-dynamic analysis campaign (differences from the baseline layout highlighted in bold).	54

Table 3.14: Summary of geometric characteristics of case 1, case 2, and case 3 layouts.	60
Table 3.15: Summary of discretization features of case 2 and case 3 geometries.	60
Table 3.16: Examined t_1 values.	62
Table 3.17: Summary of discretization features for parametric analysis on t_1 .	62
Table 3.18: Estimated normalized pressure loss in annular channel for different thickness values.	64
Table 3.19: Estimated and inferred normalized pressure losses in annular channel for different thickness values.	65
Table 3.20: Pressure drop breakdown (fraction of the total pressure drop) in case 2 pin.	68
Table 3.21: Analysed combinations of parameters t_1 and r_1 .	75
Table 3.22: Summary of discretization features for parametric analysis on r_1 on double coolant flow pass pin configurations.	76
Table 3.23: Examined r_1 values in single coolant flow pass pin configurations.	78
Table 3.24: Summary of discretization features for parametric analysis on r_1 in single coolant flow pass pin configurations.	78
Table 3.25: Estimated normalized pressure loss in outer annular channel for different thickness values.	81
Table 3.26: Geometric features of the case 13 and case 15 layout.	83
Table 4.1: Association between loading conditions and damage criteria for DEMO [69].	98
Table 4.2: RCC-MRx criteria verification - case 2 layout.	121
Table 4.3: RCC-MRx criteria verification - case 5 layout.	122
Table 4.4: Summary of discretization features for grid independence analysis.	126

List of acronyms

ACB	Advanced Ceramic Breeder
ASME	American Society of Mechanical Engineers
BB	Breeding Blanket
BC	Boundary Condition
BSS	Back Supporting Structure
BZ	Breeding Zone
CAD	Computer Aided Design
CAT	Category
CFD	Computational Fluid-Dynamics
COB	Central Outboard Blanket
DCT	DEMO Central Team
DEMO	DEMONstration fusion reactor
DPA	Displacements Per Atom
FEA	Finite Element Analysis
FEM	Finite Element Method
FVM	Finite Volume Method
FW	First Wall
GPS	Generalised Plain Strain
HCPB	Helium-Cooled Pebble Bed
IAEA	International Atomic Energy Agency
IED	Immediate Excessive Deformation
IFMIF	International Fusion Materials Irradiation Facility
IPFL	Immediate Plastic Flow Localization
IPI	Immediate Plastic Instability
KIT	Karlsruhe Institute of Technology
ITER	International Thermonuclear Experimental Reactor
MCNP	Monte Carlo N-Particle
MFR	Mass Flow Rate
NH	Nuclear Heating
PCD	Pre-Conceptual Design
PCS	Power Conversion System

PDE	Partial Differential Equation
PHTS	Primary Heat Transport System
R&D	Research and Development
TBR	Tritium Breeding Ratio
TF	Toroidal Field
TRL	Technology Readiness Level
VV	Vacuum Vessel
WCLL	Water-Cooled Lithium-Lead
WLCB	Water-Cooled Lead Ceramic Breeder

Abstract

Nuclear fusion, the reaction that powers the Sun and the stars, has the potential to be the long-term solution to meet the growing energy demand arising from human activities. In a nuclear fusion reactor, one of the most critical components is the breeding blanket (BB). In fact, this system serves as a key component for recovering the thermal power produced by fusion, breeding tritium for plant self-sufficiency, and shielding the components outside the vacuum vessel from fusion neutrons.

The European fusion roadmap identifies the development of the European DEMOnstration Power Plant (DEMO) as one of the main steps towards the realisation of industrial fusion power plants, and widespread consensus in the scientific community indicates the DEMO BB as one of the systems requiring more focused attention.

The Helium-Cooled Pebble Bed (HCPB) BB is one of the two concepts that are currently under investigation to be selected as driver blanket for DEMO and adopts gaseous helium as coolant and tritium carrier together with solid tritium breeder and neutron multiplier. Its main limitations regarding manufacturability, reliability, and safety can be significantly mitigated by embedding the functional materials within actively cooled pin-like structures, assembled in a monoblock architecture not featuring a typical first wall component. The described configuration is, thus, a variant of the reference HCPB, and is called HCPB monoblock BB.

The present PhD Thesis details the first studies addressing the thermofluid-dynamics and thermomechanics of the HCPB monoblock. In more detail, the thermofluid-dynamics of a pin was optimised with an extensive campaign of Computational Fluid-Dynamic (CFD) analyses, while its thermomechanics was investigated with an exploratory study.

The CFD analyses allowed to assess the thermal field within solid and fluid domains and the coolant total pressure drop of several pin layouts, under different thermal loading conditions. In this study, the same nuclear heating distribution – relevant to a similar concept – was mapped onto all the different pin layouts, thus assuming fixed neutronics.

The optimisation allowed to detect multiple design options either complying with all the requirements in terms of operational material temperatures and coolant pressure drop or only overcoming the temperature limits by narrow margins. Hence, this study demonstrated the thermal and fluid dynamic viability of the HCPB monoblock.

Two different configurations among the most performative were selected for the exploratory thermomechanical study, with the aim to assess the HCPB monoblock viability

from the structural standpoint. In these evaluations, the thermal fields estimated by CFD have been adopted.

The analysis showed a satisfactory behaviour of the monoblock architecture envisaging a honeycomb arrangement of the pins. As for the pins, the structural design criteria indicated an optimal response when solely considering the primary stresses, while some criticalities emerged when thermal loading was also taken into account. However, the critical issues appear to be localized and potentially solvable without compromising the favourable thermal and fluid-dynamic performance of the optimized pin layouts. Hence, the overall structural behaviour of the HCPB monoblock can be regarded as rather encouraging.

Introduction

Nowadays, the effects of global warming appear to be increasingly evident, spanning from record-high temperatures to an increased frequency of extreme climatic events. According to the Copernicus Climate Change Service, August 2025 was the third-warmest on record [1], while 2024 is confirmed to be the warmest year on record globally, and the first calendar year that the average global temperature exceeded 1.5°C above its pre-industrial level [2]. These are just some of the most recent data, extracted from trends showing a long history of global average temperature increases with time. Nature is showing the need for a steep reduction in greenhouse gas emissions from mankind, on the other hand, the human population continues to grow, leading to greater resource consumption. The World Energy Outlook 2024 [3] assumes the global population to expand from 8 billion people in 2023 to 9.7 billion in 2050, with a stated policy scenario (STEPS) of 0.5% growth in global energy demand per year on average between 2023 and 2035. Accordingly, the STEPS exhibit an increase in global CO₂ emissions up to 2030, in contrast to the sustainable development scenario – the one that allows achieving the goals agreed in Paris in 2015 – which requires a reduction in CO₂ emissions in the same period.

In this context, scientific investigation of alternative and low-carbon energy sources to complement renewables represents a crucial step to pursue the path to a sustainable future. To this end, the exploitation of the Deuterium–Tritium (D-T) nuclear fusion reaction for the production of electricity appears to be a very promising option, and thus the international scientific community is investing a large amount of manpower and financial resources to achieve the commercial production of electricity from nuclear fusion on a large scale.

Currently, the European Union is performing fusion research following the *European Roadmap to the Realisation of Fusion Energy* [4], defined by the European Commission in 2012 and then updated in 2018. This program is pursued by the EUROfusion Consortium, which groups 30 member institutes and over 150 associated entities from across Europe, including the UK, Switzerland, and Ukraine. The first step of the roadmap is the construction of the International Thermonuclear Experimental Reactor (ITER), with the scope to demonstrate the scientific and technological feasibility of fusion as a reliable energy source. The operation of ITER will allow researchers to collect data to be used for the development

of the DEMONstration Power Plant (DEMO) that will meet the goal of delivering a net electricity output (300-500 MW) to the grid.

At present, the DEMO project is in its conceptual phase. After a pre-conceptual design phase, which ended in 2020, that allowed the selection of a credible baseline while highlighting the major technological concerns, the conceptual phase has started with the aim of selecting, for each system, the most promising technological option for the next engineering phase. Thus, the conceptual design phase and the definition of the final layout will lead to DEMO construction and operation, planned for the end of this century.

To ensure that the D-T fusion reaction happens with an acceptable reaction rate – that is the number of reactions taking place per unit time and volume – the two isotopes must reach temperatures of 70-100 million degrees Celsius. In these conditions, the fusion fuel exists in the fourth state of matter, that is plasma, an ionized gas composed of electrons and free ions which is globally electrically neutral. Since no structural material currently known can withstand the high temperatures of a D-T plasma, it must be confined to avoid any direct contact with the reactor walls. ITER and DEMO are based on the *tokamak* reactor concept, where the plasma is confined and controlled magnetically, using a complex system of superconducting coils.

DEMO will have two major additional goals with respect to ITER: electricity production and fuel self-sufficiency. In fact, while deuterium can be extracted from seawater in virtually boundless quantities, tritium is radioactive and is present only in trace amounts on Earth. According to the Roadmap, tritium will be externally supplied for ITER, while in DEMO it will be bred directly inside the reactor, through the so-called Breeding Blanket (BB).

In this component, the neutrons produced by fusion reactions will interact with the lithium contained therein, producing tritium. Furthermore, the BB must provide proper radiation shielding to the Vacuum Vessel and the superconducting coils, and it must allow the extraction of both the plasma-radiated and neutron-deposited heat power that will be converted, downstream several steps, into electric power to be delivered to the grid. In addition to that, the BB must withstand severe operating conditions. For these reasons, the development of a robust BB system is crucial for the design of the whole DEMO reactor. However, the International Atomic Energy Agency (IAEA), currently estimates the technology readiness level (TRL) of the DEMO BB to be rather low, namely TRL4 [5], and the BB is identified, among the systems of DEMO, to be one of the most critical and inter-related technological challenges that requires a more focused attention [6].

Within the Work Package Breeding Blanket of the EUROfusion consortium, two BB concepts are currently under investigation to be selected, at the end of the conceptual phase

[7], as driver blanket concepts for the DEMO engineering phase: the Water-Cooled Lithium-Lead (WCLL) BB [8] [9] [10] and the Helium-Cooled Pebble Bed (HCPB) BB [11] [12]. Nevertheless, evaluations are also ongoing about hybrid variants between these two configurations, such as the Water-Cooled Lead Ceramic Breeder (WLCB) [13].

The WCLL considers subcooled pressurized water as coolant and an eutectic alloy of lithium-lead as breeder and neutron multiplier, whereas the HCPB foresees gaseous pressurized helium as coolant and a solid breeder, lithium ceramic compounds in form of pebble beds, and neutron multiplier, beryllium in form of pebble beds or solid blocks. In the WLCB, instead, a Lead/Lead-alloy is used as neutron multiplier, ceramic pebbles as tritium breeder, and pressurized sub-cooled water as coolant.

Regardless of its nature and coherently with the magnetic confinement system arrangement, the DEMO BB is divided into 16 toroidal sectors, each covering 22.5° of the torus, as envisaged by the latest DEMO baseline DEMO BL2017 [14]. Within each BB sector, 5 BB segments can be recognised: 3 in the outboard region (Left, Central, and Right) and 2 in the inboard one (Left and Right).

Currently, the DEMO Central Team (DCT) is investigating alternative configurations of the baseline WCLL and HCPB BB concepts, as well as the hybrid concept WLCB, aiming to address the risks identified at the end of the Pre-Conceptual Design (PCD) [15] phase or to explore "high-risk, high-reward" designs [16] [17].

The research activities conducted during the three years of the XXXVIII Cycle PhD course in *Energy*, curriculum *Low Carbon Energetics and Innovative Nuclear Systems*, held at the University of Palermo, are framed within DEMO conceptual design activities and were 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 was particularly focused on the thermofluid-dynamic and thermomechanical study of innovative DEMO BB concepts.

The present Thesis work consists of the thermofluid-dynamic optimization and of an exploratory thermomechanical study on a variant of the reference HCPB BB concept, the so-called *HCPB monoblock* BB. Alike the reference HCPB BB, the HCPB monoblock adopts gaseous helium at 8.0 MPa as coolant and tritium carrier together with solid tritium breeder and neutron multiplier. However, this variant envisages all the functional materials to be contained in actively cooled pin structures, which allows to overcome safety concerns regarding high temperatures in case of cracking of the neutron multiplier. Moreover, the HCPB monoblock is characterised by the absence of a typical FW, which works around the

necessity of a prohibitive number of welds that negatively impacts the manufacturability and reliability of the reference HCPB BB [11].

The main objective of the thermofluid-dynamic studies was the assessment and optimization of the thermal and fluid-dynamic behaviour of a pin of the HCPB monoblock, with the purpose of achieving a pin geometric configuration characterized by acceptable structural and functional material temperatures, with an acceptably low coolant pressure drop. In these research studies, the sensitivity of the pin performance to different parameters has been examined under multiple working conditions. This investigation has been performed following a theoretical-computational approach based on the Finite Volume Method and adopting the commercial Computational Fluid Dynamics (CFD) code Ansys Fluent 2023 R2 [18], where a 2D-axisymmetric model has been set up, allowing to perform over 350 analyses with a manageable computational burden.

The main objective of the exploratory thermomechanical study was to assess the capability of an HCPB monoblock pin to withstand the thermomechanical loads arising from DEMO *operational* and *expected* loading scenarios, by checking against the level A RCC-MRx structural design criteria [19]. To this scope, the geometric pin layouts characterized by the lowest structural material temperatures and an acceptable coolant pressure drop have been examined. This part of the investigation has been performed following a theoretical-computational approach based on the Finite Element Method and adopting the commercial code Ansys Mechanical 2023 R2 [20]. To assign realistic boundary conditions to the pin, a *submodel* approach has been adopted. In this multi-level strategy, first “global models” have been set up to examine the overall structural performances of the equatorial region of the HCPB monoblock Central Outboard Blanket (COB) segment. Then, in the second part, “sub models” of a single pin have been developed and used to assess in detail the structural performances of the pin internals while taking into account the mutual actions exchanged with the rest of the cluster in the COB segment, and thus allowing to perform the design criteria verification according to the RCC-MRx standard. Grid independence evaluations have been performed for both thermofluid-dynamic and thermomechanical studies.

The thesis is structured as follows: Chapter 1 presents some generalities on nuclear fusion physics, DEMO reactor and its BB system with a final focus on the HCPB monoblock. Chapter 2 introduces the parameters and working conditions that have been considered in the thermal and fluid-dynamic pin optimization. Chapter 3 illustrates models, loads, and boundary conditions of the thermofluid-dynamic optimization, together with the results and outcomes obtained. Finally, Chapter 4 details the thermomechanical study, illustrating models, loads, boundary conditions, and results.

Chapter 1 The HCPB monoblock Breeding

Blanket of the DEMO Fusion Reactor

1.1 Elements of fusion reaction physics

Nuclear fusion is an exergonic reaction in which two light nuclei coalesce, producing a highly unstable compound nucleus that splits into a heavier stable nucleus and a nucleon. The resulting nucleus is more bound compared to the reacting nuclei due to the increasing forces per nucleon. As shown in Figure 1.1, the light nuclides are characterized by a smaller average binding energy per nucleon, which means that they are less tied and, therefore, thermodynamically less stable. As a consequence, under appropriate conditions, they will transit into the more stable central zone by means of the fusion reaction [21].

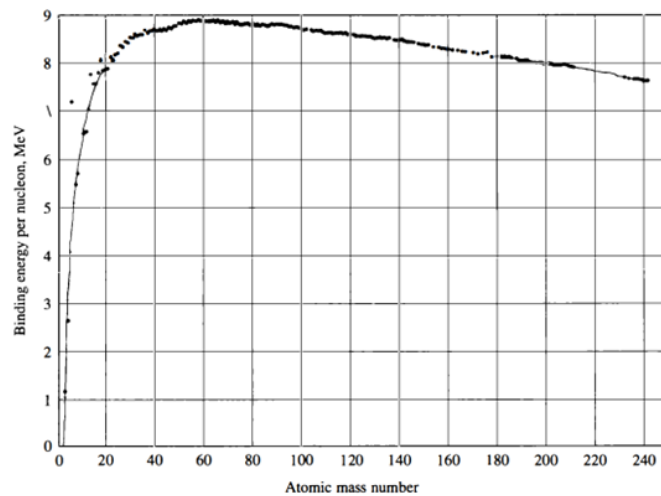


Figure 1.1: binding energy per nucleon as a function of atomic mass number [22].

A fusion reaction occurs under the predominant action of two opposing forces: the strong nuclear force and the electrostatic force, two of the four basic forces in nature. The strong nuclear force is an attractive, threshold force, i.e., negligible if the distance between particles is greater than a given threshold distance R_0 . On the other hand, the electrostatic force F_e is attractive for particles of unlike charge and repulsive for particles of like charge, and it depends on their relative distance as determined by Coulomb's law (Eq. 1.1):

$$F_c = \frac{1}{4\pi\epsilon_0} \frac{q_a q_b}{r^2} \quad (1.1)$$

where ϵ_0 is the permittivity of free space, q_a and q_b are the charges of the two generic interacting a and b particles, and r is their relative distance.

The work done in moving a particle of charge q_a from a sufficient distance (ideally infinite) to within a distance r of a stationary particle of charge q_0 is, by definition, the potential energy $U(r)$ associated with the resultant charge configuration (Eq. 1.2)

$$U(r) = \int_{\infty}^r F_c(r') dr' = \frac{1}{4\pi\epsilon_0} \frac{q_a q_0}{r} \quad (1.2)$$

The potential energy $U(R_0)$ associated with the threshold distance R_0 is referred to as the Coulomb barrier. Considering the electrostatic force only, this is the minimum kinetic energy an incident particle would have to possess in order to overcome the Coulomb barrier. If particles reach a sufficiently small distance from each other (i.e., $r < R_0$), the attractive strong nuclear force dominates over the electrostatic force, and a compound nuclear state is formed. However, the quantum phenomenon known as “tunnel effect” implies a non-vanishing probability that two interacting nuclei overcome mutually their electrostatic repulsion while having lower energy than the threshold one (Figure 1.2).

The Coulomb barrier for light particles like deuterium and tritium is around 0.4 MeV, therefore, to obtain fusion reactions happening with adequate frequency for power production purposes, it is necessary to heat a hydrogen gas up to temperatures for which enough nuclei have energies of relative motions greater than $U(R_0)$. However, due to the previously introduced tunnelling effect, an average kinetic energy of the relative motion of the reactant nuclei on the order of 10-20 keV, is sufficient.

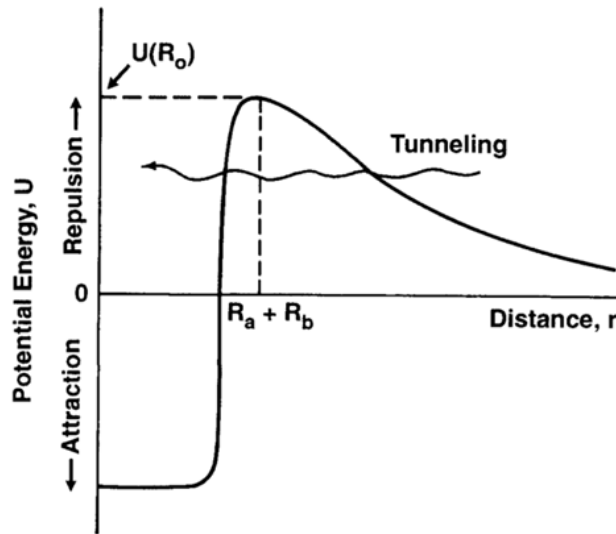


Figure 1.2: Depiction of electrostatic repulsive potential ($r > R_0$) and nuclear attraction ($r < R_0$) [23].

The corresponding temperatures are in the order of $\sim 10^8$ K, in which the gas is in the plasma state since hydrogen has an ionization potential of 13.6 eV.

The fusion reaction energy output is proportional to the mass defect $\Delta m = m_{\text{products}} - m_{\text{reactants}}$ due to the principle of conservation of energy (Eq. 1.3).

$$Q = (-\Delta m)c^2 \quad (1.3)$$

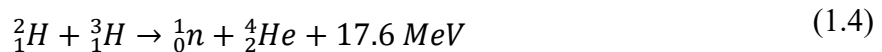
Where Q is the energy output of the reaction, and c is the speed of light in vacuum (i.e., 299,792,458 m/s). It is evident that energy is released by the reaction ($Q > 0$) only if $\Delta m < 0$, as in the fusion reaction: in this case, the reaction is called exoergonic; otherwise, it is called endoergonic.

Several reactions with a positive energy output have been identified that could be useful on Earth. Some of the most relevant are shown in Table 1.1, where h is chosen to represent the helium-3 nucleus (${}^3\text{He}^{2+}$) and P is the proton.

Table 1.1: Principal nuclear reactions [24].

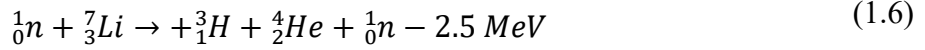
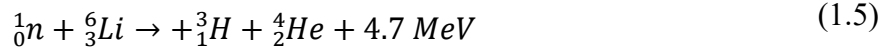
Reaction name	Reaction	Q-value [MeV]
D-T	$D + T \rightarrow \alpha + n$	17.6
(D-D) _p	$D + D \rightarrow t + p$	4.05
(D-D) _n	$D + D \rightarrow h + n$	3.27
D-h	$D + h \rightarrow \alpha + P$	18.34
D- ${}^6\text{Li}$	$D + {}^6\text{Li} \rightarrow 2\alpha$	22.40
P- ${}^7\text{Li}$	$P + {}^7\text{Li} \rightarrow 2\alpha$	17.20
P- ${}^6\text{Li}$	$P + {}^6\text{Li} \rightarrow \alpha + h$	4.00

Not all of these reactions are suitable as fuels for a fusion power plant, for example, at a plasma pressure of 1 MPa, D-T fuel can produce a power density of 6 MW/m³, while the D-D reaction can produce only 0.2 MW/m³. Moreover, the D-T fuel has the least demanding physics conditions to reach ignition [25]. In this context, the fusion reaction, which is expected to be the first to be exploited as a fuel for fusion power plants, is the D-T reaction (Eq. 1.4).



While deuterium for the D-T reaction is abundant (Earth waters have a mean particle density ratio of 1 deuterium particle over 6700 hydrogen atoms), tritium is scarce; it is a radioactive β emitter with a half-life of 12.3 years.

The main source of tritium in future nuclear fusion power plants is expected to be lithium, through nuclear reactions referred to as tritium breeding. It occurs by capture of the fusion neutron in lithium according to the reactions in Eq. (1.5) and (1.6).



Lithium-6 and lithium-7 are naturally occurring stable isotopes existing with 7.5% and 92.5% isotopic abundance, respectively, and exist in considerable quantities on Earth; therefore it is possible to breed tritium from lithium and use it as a fuel. For reactors like DEMO, it is expected to make these reactions occur in a blanket surrounding the fusion core, the so-called Breeding Blanket. It is necessary that for every fusion reaction, at least one tritium breeding reaction will follow. A Tritium Breeding Ratio (TBR) can be defined as the ratio of the rate of tritium production in the breeding blanket to the rate of tritium burned in plasma. TBR has to be greater than 1 to ensure the reactor self-sufficiency, with a minimum value ranging from 1.1 to 1.2.

Thermonuclear fusion is the most promising approach for exploiting D-T fusion reactions for power production. The process consists of the confinement and heating of a population of deuterium and tritium atoms in a given space, forming a plasma which is expected to attain thermodynamic equilibrium as a result of random collisions. The resultant spectrum of particle energies is then well described by a Maxwell-Boltzmann distribution, with the high energy part of this distribution providing for most of the desired fusion reactions.

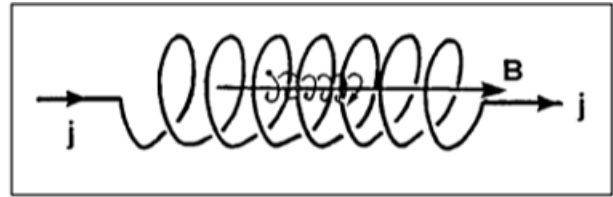
The critical technical constraint is the sustainment of a sufficiently stable high-temperature (10^8 K) plasma in a practical reaction volume and for a sufficiently long period of time to make the entire process energetically possible.

Confinement of the fuel ions is thus crucial to maintain these conditions within the required reaction volume. Several methods can be used to confine plasma (electrostatic confinement, material confinement, gravitational confinement, inertial confinement). However, the most promising strategies to exploit fusion reactions are the inertial and magnetic confinements [23].

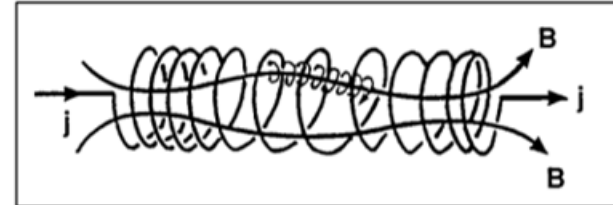
The oldest magnetic confinement devices were the solenoidal fields, but they proved not to apply to fusion power plants due to the high particle leakage occurring at the ends of the solenoid. Leakage could be reduced by using a mirror magnetic field, that is a magnetic field whose strength is enhanced at each end of the cylindrical solenoid region. However, the best

solution to avoid leakage is to eliminate the ends, which means closing the solenoidal field in order to achieve a toroidal field (TF) (see Figure 1.3) [23].

a) Solenoidal B-field



b) Mirror B-field



c) Toroidal B-field

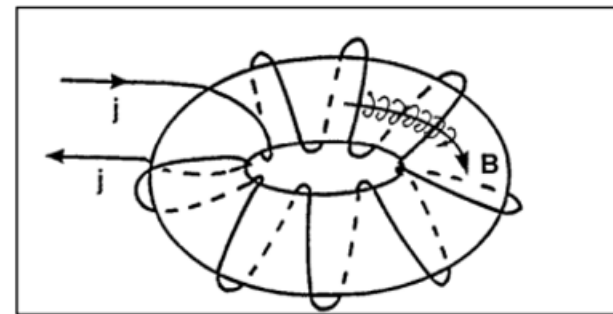


Figure 1.3 (a,b,c): Depiction of the three magnetic field topologies and illustrative ion trajectories.

The two main devices that exploit a toroidal field are tokamaks and stellarators (see Figure 1.4). Tokamak is a Russian acronym for toroidal chamber with an axial magnetic field, and it has become the favoured concept of toroidal fusion reactor. It was invented by Russian physicists Sakharov and Tamm in 1951 and was first experimented at the Kurchatov Institute (Moscow) in the same year.

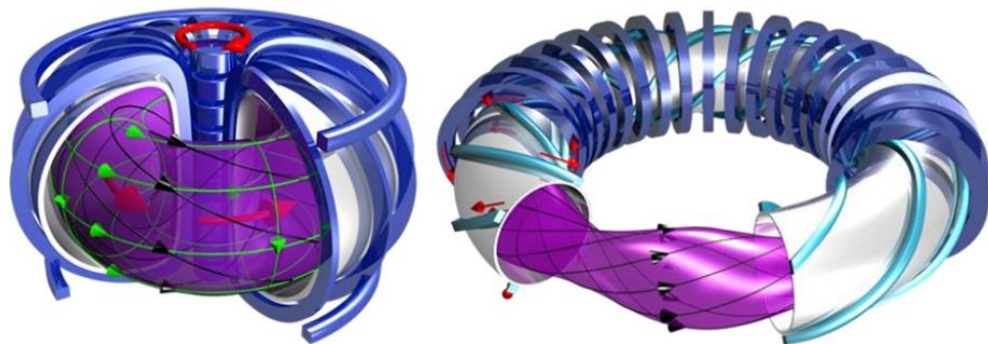


Figure 1.4: Conceptual representation of a tokamak (left) and stellarator (right) device.

Both in tokamaks and stellarators, the toroidal field is produced by simply passing a current through a coil wound around the torus. The coils around the torus are more closely spaced on the inboard side than on the outboard side, creating therefore a radial variation in

the magnetic field which induces a drift velocity to the particles. The net result of the drift is a local charge separation, thereby generating a vertical electric field, causing simultaneous ion and electron drift in an outward direction perpendicular to the major torus axis. Thus, the plasma in a simple toroidal field drifts outward until it strikes the surrounding wall, and there is no radial equilibrium and practically no confinement.

To avoid this effect, it is necessary to apply a poloidal magnetic field. Depending upon the relative magnitude of both poloidal and toroidal magnetic fields, a helical path of particle migration results. As a result, the particles spend equal time in the upper and lower halves of the toroid, thus cancelling out the undesired charge separation.

Stellarators and tokamaks adopt different strategies to produce the poloidal magnetic field. In a stellarator device, the poloidal magnetic field is obtained by arranging outside the plasma a set of poloidal coils which generate a current in the toroidal direction. Instead, tokamaks employ a toroidal current flowing in the plasma, which is a relatively easier solution that makes tokamaks of greater current interest.

In a tokamak (Figure 1.5), the plasma torus is viewed as a single-winding secondary of a transformer. The current flow in the primary transformer induces the current in the plasma torus. The resultant toroidal plasma current provides for Ohmic heating and also generates the poloidal magnetic field. Furthermore, stabilizing coils are installed to produce a vertical magnetic field to restrain the plasma from expanding, as is the tendency of a current ring.

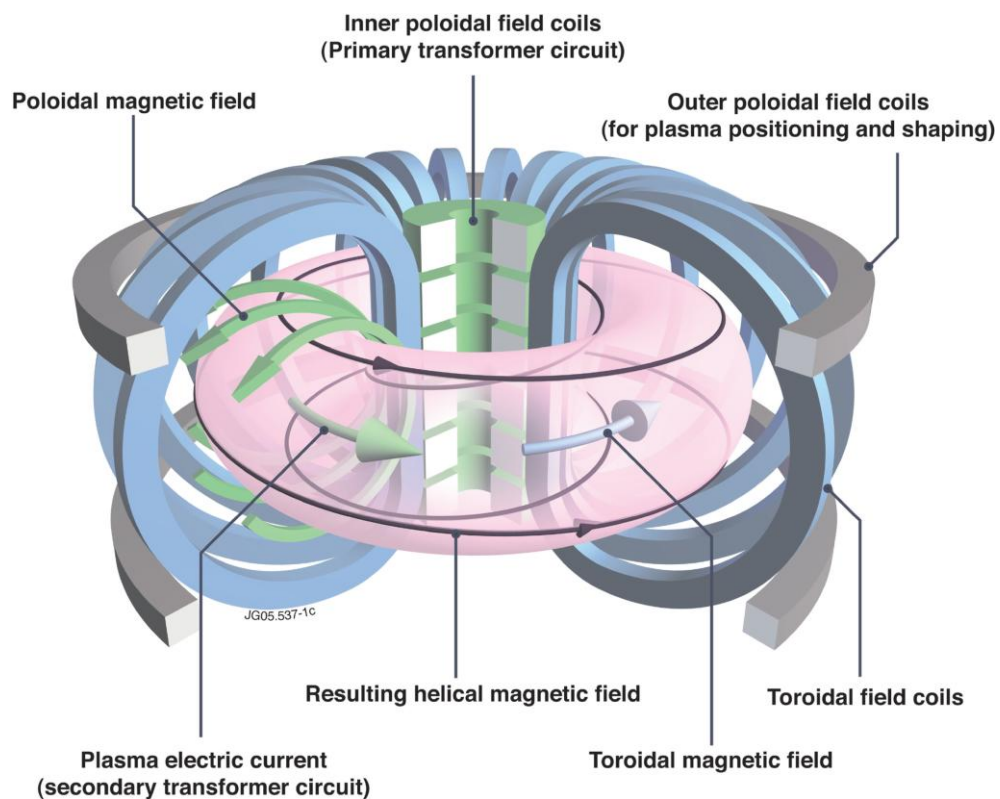


Figure 1.5: Simplified scheme of a tokamak fusion device [26].

The vertical magnetic field produces an inward-directed Lorentz force, which prohibits the outward expansion of the plasma. The toroidal plasma current needed to supply the poloidal magnetic induction is generated by a toroidal electric field, which is achieved by the time dependence of the magnetic flux that penetrates the hole in the torus. It follows, then, that a tokamak cannot operate in a steady state but only as a pulsed device unless non-inductive schemes of plasma current drive can be applied [23]. From a design point of view, all the coils are superconducting in order to minimise the dissipation of energy.

Fusion research has been centred on the idea of tokamak confinement for the past 60 years, and this development led to the projects of ITER – the world’s largest tokamak – and its successor, the DEMO reactor.

In this context, the ambitious goal of demonstrating electricity production by 2050 is the main purpose of the European fusion roadmap (Figure 1.6) [4], which has been based on three elements:

- Building and operation of the ITER test fusion reactor as the essential step toward energy production on a fast track.
- Building and operation of DEMO as a single step between ITER and a commercial power-producing fusion reactor.
- Building and operation of the International Fusion Materials Irradiation Facility (IFMIF) for material qualification under intense nuclear irradiation, in parallel with ITER.

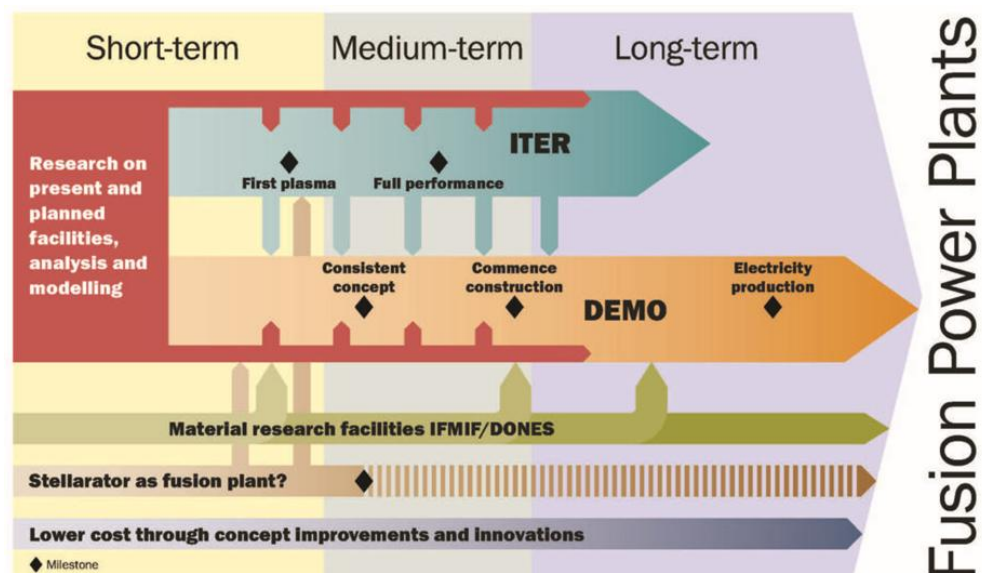


Figure 1.6: European Roadmap [4].

The roadmap reports three separate periods with distinct main objectives:

- Horizon 2020 (2014-2020) with the objective of constructing ITER within scope, schedule, and cost. During this period, the foundation for a fusion power plant has to be laid, and innovation has to be promoted in the industry.
- Second period (2021-2030) with the objective to exploit ITER up to its maximum performance in order to acquire sufficient know-how for DEMO construction.
- Third period (2031-2050), during which complete ITER exploitation will lead to the construction and operation of DEMO.

The reactor ITER, the largest burning plasma experiment, will produce 300-500 MW of power with an injected power of 50 MW, so that the fusion gain is expected to be up to 10. It has been designed with currently available materials and present technologies, and based on experimental data and theoretical studies performed over the past few decades. With its first plasma originally planned for 2025, it is anticipated that ITER will advance the knowledge database for tokamak physics and technology further enough to build the first fusion power plant with electric power production capability, DEMO, whose general goals are to produce few hundred megawatts net electricity for the grid, breed the amount of tritium needed to close its fuel cycle and demonstrate all the technologies for the construction of a commercial fusion power plant, including an adequate level of availability. To meet these goals, DEMO will have to rely on simple and robust technical solutions, well-established and reliable regimes of operation, and adequate use of materials for the expected level of neutron fluence. These requirements imply extensive ITER exploitation to extrapolate technologies as far as possible and pursue innovation both in industries and research institutions.

1.2 The DEMO fusion reactor

DEMO represents the bridge between ITER and the future power plants pointed towards the commercial application. The main goals that should be obtained through the DEMO project consist of [27] [28]:

- Demonstrating a net electricity output to the grid (from 300 to 500 MWe) with a 2GW plasma power;
- Qualifying all the fusion-relevant technologies;
- Working with a closed fuel cycle;
- Complying with all the safety requirements;
- Showing the effective environmental sustainability of the process.

The construction and then the operation of DEMO is evenly divided into three steps, well-structured by many other secondary goals. The PCD phase led to the first step being finalized

in 2020. The aim of this first stage consists of a comparison between various plant design options with a reference concept to find and select the configurations with the highest probability of success. These selected concepts define the basis for the subsequent developments (Conceptual Design phase) and at the end of that optimization, only the best configuration will be chosen for the Engineering Design phase.

In general, a fusion power plant requires a group of components (see Figure 1.7) to ensure that plasma will be efficiently and safely controlled, and furthermore that the energy output will be satisfactory. To achieve these goals, DEMO architecture will be composed of different systems, listed below [29]:

- A magnet system, which consists of superconducting coils that are actively cooled with helium at 4 K. These coils generate the magnetic fields required to confine, shape, and control the plasma. Specifically, the system includes the toroidal field coils, poloidal field coils, and the central solenoid.
- A Vacuum Vessel (VV), namely a toroidally shaped pressure vessel which provides mechanical support for the in-vessel components, and neutron shielding for the superconducting magnets.
- A Cryostat, which contains the VV and the magnets, providing a cryogenic environment at low pressure. It is shielded from the VV by a thermal shield, and it transfers the loads from the tokamak to the reactor base.
- A Breeding Blanket, which covers the inner surfaces of the VV, performing the core functions of removing the thermal power deposited by plasma radiation and transferring it to the power conversion system, shielding the VV and the magnets from radiation, and producing tritium for the plant's self-sufficiency.
- A Divertor, which is located in the lower part of the VV, and is a high heat flux component in charge of collecting particles and energy exhausted by the plasma.
- Ports, located in the upper, lower, and equatorial regions of the VV in order to provide access to the in-vessel components for maintenance, diagnostics, and fuelling systems;
- Heat transfer and conversion system: in case of HCPB BB, the chain of heat transfer/conversion systems includes the Primary Heat Transport System (PHTS, using Helium as process fluid, Figure 1.8), an Intermediate Heat Transport System equipped by an energy storage system where energy is buffered for dwell operation and finally the Power Conversion System (PCS, Rankine Cycle). In case

of WCLL BB, instead, the PHTS (water operated) is directly thermally connected to the PCS [30].

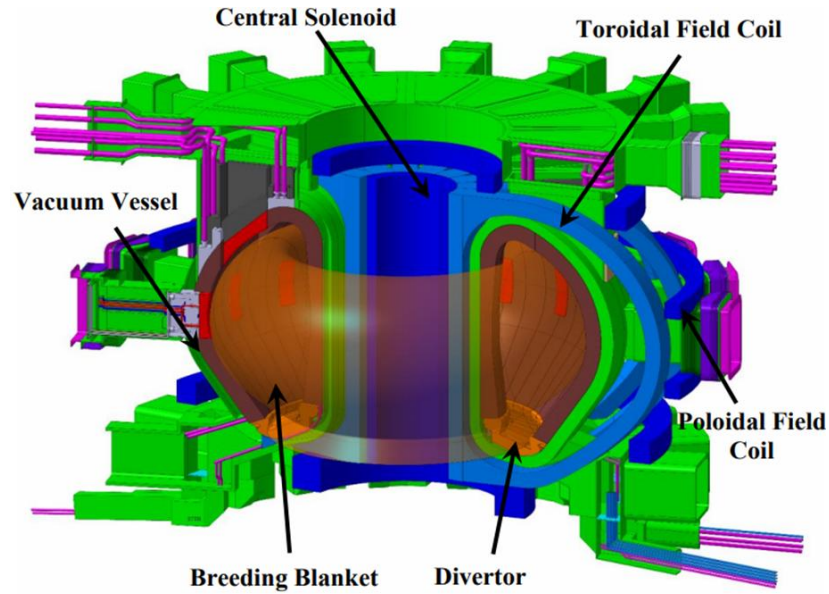


Figure 1.7: Representation of the main components of DEMO [29].

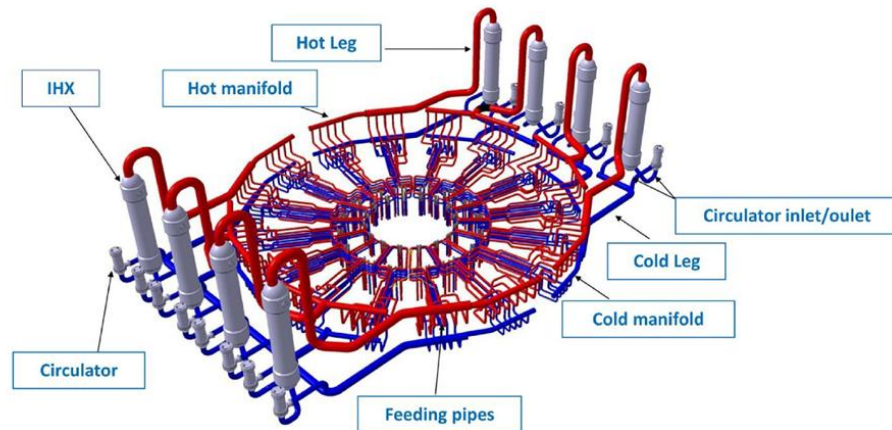


Figure 1.8: Three-dimensional layout of the HCPB BB PHTS [31].

The current DEMO baseline is the DEMO BL2017 design (Figure 1.9), which has a major radius of 9m and a minor radius of 2.9m, and it has 16 toroidal field superconducting coils, dividing the machine into 16 sectors [32].

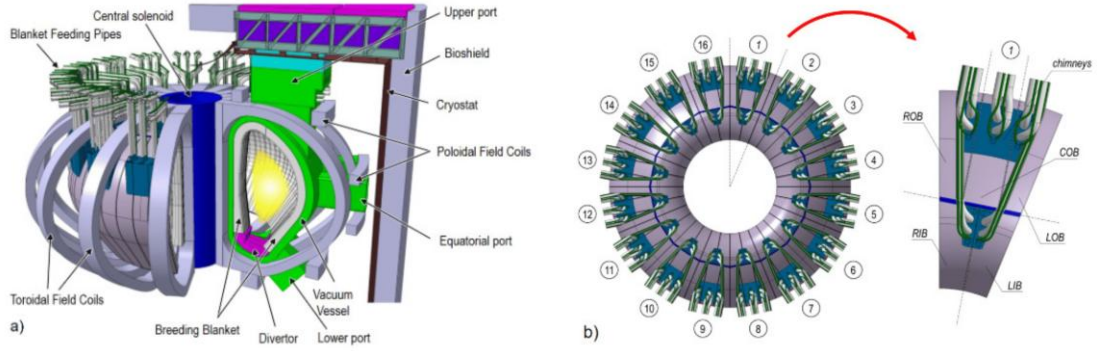


Figure 1.9: DEMO BL2017 configuration: (a) IsoView of DEMO BL2017; (b) top view of DEMO BL2017 and blanket sector [11].

The BB is a component of crucial importance for meeting the goals of the DEMO reactor, as it is devoted to capturing the power emitted by plasma and producing tritium (other than radiation shielding). At the end of the PCD phase, the WCLL BB and the HCPB BB emerged as the most promising BB concepts, hence the Conceptual Design is now focused on their development and on the exploration of possible hybrid solutions (WLCB BB) or variants of their baseline layouts, in view of a final decision for the driver blanket of DEMO.

The present Thesis work is focused on the analysis of a variant of the HCPB BB, conceived to overcome its critical issues and potential showstoppers. Hence, in the following, the HCPB BB is illustrated, and finally, the innovative HCPB monoblock BB is introduced.

1.3 The HCPB Breeding Blanket: design evolution, status and limitations

The HCPB blanket concept uses pressurized helium gas (at 8 MPa) as a coolant, lithium ceramic as a tritium breeder, beryllium-containing material as a neutron multiplier material, the reduced-activation ferritic martensitic steel Eurofer97 as structural material, and tungsten as armor on the first wall (FW). The tritium bred in the HCPB BB will be extracted through a purge gas system [11]. The HCPB line has been led by the Karlsruhe Institute of Technology (KIT, formerly known as KfK and FZK) in Germany since the 1980s ([33] - [41]).

During the PCD phase, the reference HCPB BB design has evolved, passing from a *beer box* concept to a *sandwich* configuration – both of which were studied in the framework of the DEMO baseline DEMO BL2015 – and finally to the *fuel breeder pin* arrangement (see Figure 1.10), conceived in the context of the DEMO BL2017. Summarising, the *beer box* concept was a robust design but had the disadvantage of envisaging a large amount of steel in the breeder zone, compromising the TBR. On the other hand, the *sandwich* configuration

was affected by the manufacturing complexity of the small channels embedded in the cooling plates, and the large coolant pressure drop observed thereby.

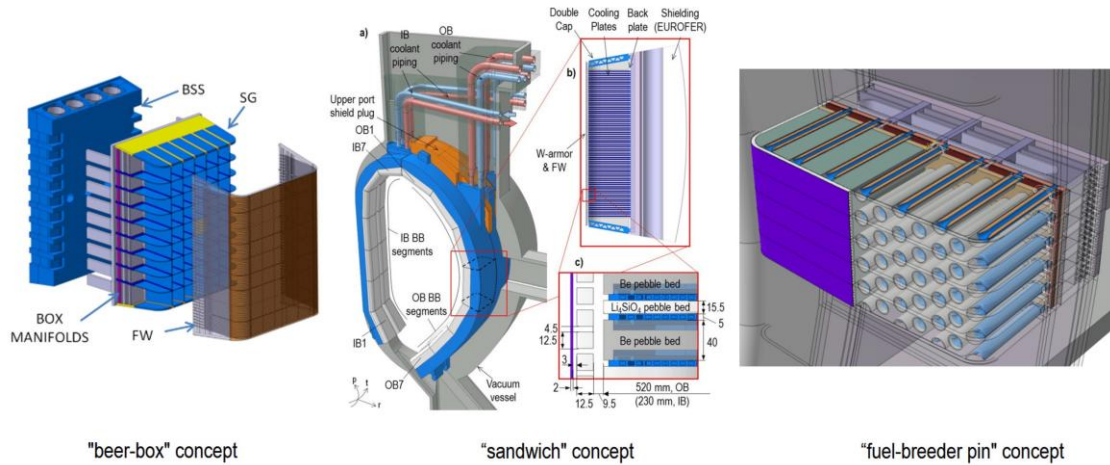


Figure 1.10: Design evolution of HCPB BB during PCD phase [11].

The *fuel-breeder pin* concept currently represents the reference HCPB design. Relevant updates introduced in the *fuel-breeder pin* concept included the change in the functional materials. The tritium breeder material Li_4SiO_4 was replaced with the advanced ceramic breeder (ACB), which is a solid solution of two-phase materials consisting of Li_4SiO_4 and about 35% mol. Li_2TiO_3 . The ACB tritium breeder, which is also known as KALOS, is in the form of pebbles with an average diameter of 750 μm instead of the previous 350 μm of Li_4SiO_4 . This kind of lithium ceramic material developed at KIT during the 2010s [42] has a better crush load and tritium release performance.

The neutron multiplier beryllium pebble was replaced with a beryllide (TiBe_{12} as reference material and CrBe_{12} as backup) block. Pure beryllium, in fact, posed several issues, among which is that a significant fraction of the generated tritium (up to 100% below 500°C) is trapped within helium bubbles, which is a safety concern [43]. Moreover, pure beryllium poses a risk in the case of a postulated accidental scenario involving water ingress (from failed water-cooled divertors and/or limiters) or air ingress into a failed blanket box exposing beryllium material [11]. On the other hand, the use of beryllide allows to adopt new configurations other than the pebble beds, such as slabs or blocks, thanks to the very low swelling. Exploiting this property of beryllide, the *fuel-breeder pin* concept envisages hexagonal prismatic beryllide blocks, as visible in Figure 1.11 [41].

In the *fuel-breeder pin* concept, there are two concentric tubes, a small one and a large one, forming the cladding of the ACB pebbles. On one side, the two tubes are connected by a funnel, and the space between them is filled with the ACB pebbles. Outside of these two concentric tubes, there is a larger concentric pressure tube, forming the annular cooling channel together with the cladding [11].

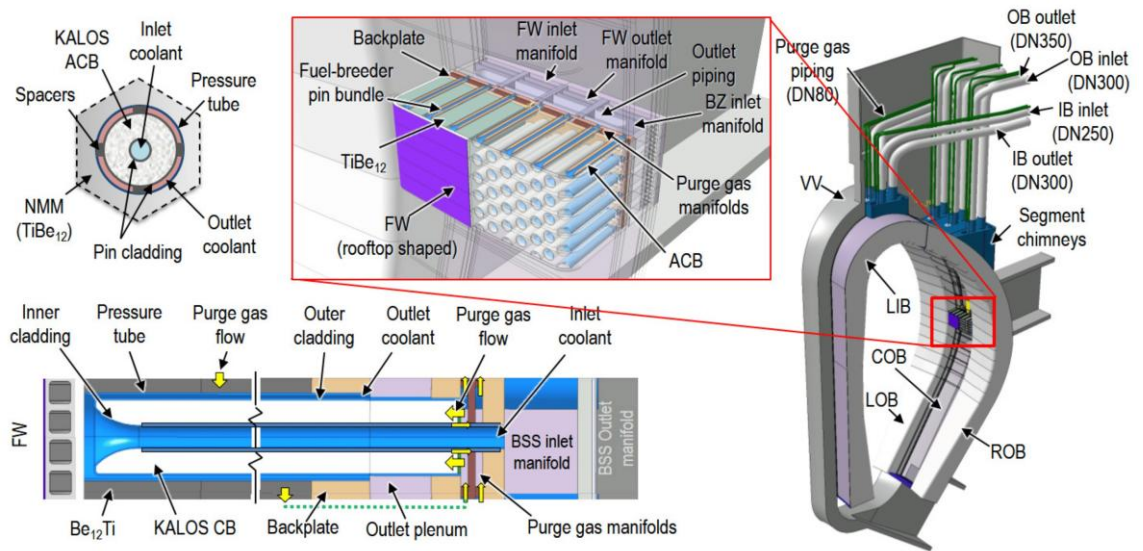


Figure 1.11: Fuel-breeder pin concept of the HCPB BB in 2018 [11].

In light of CFD studies [44], the funnel shape was inverted in order to achieve higher flow stability (Figure 1.12).

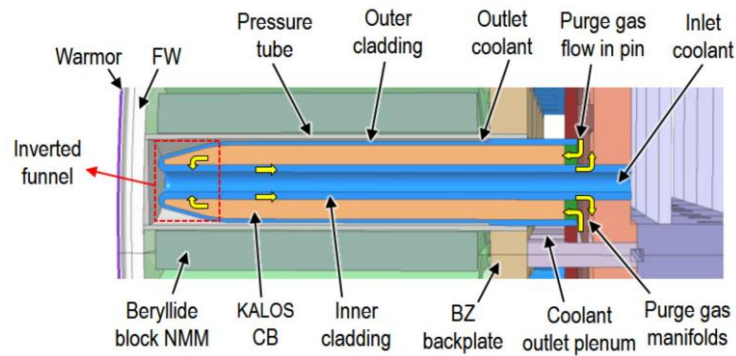


Figure 1.12: Detailed view of a fuel-breeder pin with inverted funnel shape [11].

The Computer Aided Design (CAD) of the HCPB BB at the end of the PCD phase is depicted in Figure 1.13. The outer contour of the blanket segment is formed by the stiff FW and the back-supporting structure (BSS) made of Eurofer97. The FW has a toroidal rooftop shape, which is introduced to protect the leading edge of the FW, similar to that of the ITER FW [32]. Behind the FW, there is the breeder zone, which comprises continuous arrays of triangularly arranged fuel-breeder pins surrounded by beryllide hex blocks (see Figure 1.13) (A-A). At the upper region of the segment, the coolant feeding/collecting and purge gas pipes connected to the segment chimneys are routed in and out through the upper port of the VV. The blanket segments are attached to the VV through attachment structures [45].

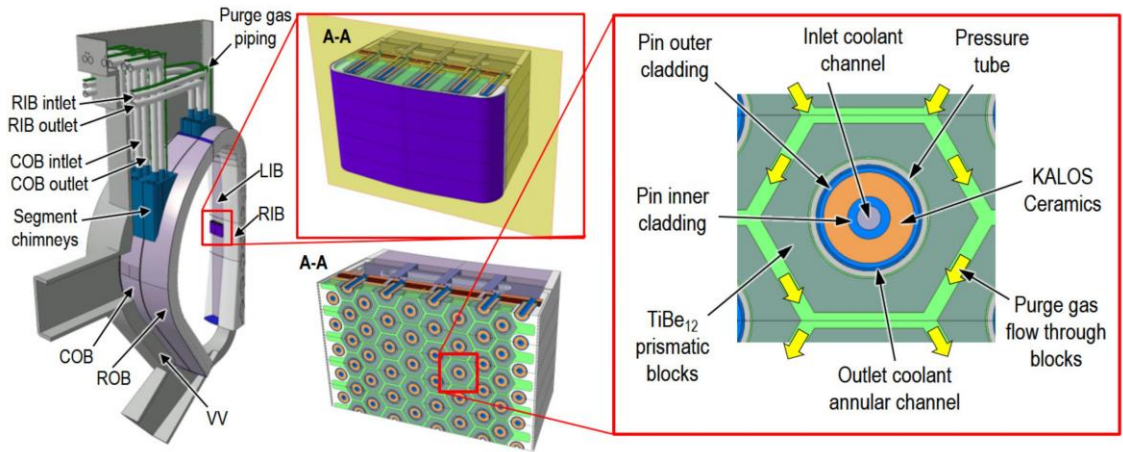


Figure 1.13: HCPB BB at the end of the PCD phase [11].

The reference HCPB BB presents some limitations. One of the major risks of the *fuel-breeder pin* concept is the possible loss of structural integrity of the prismatic Be_{12}Ti multiplier blocks, which are placed around the pressure tubes, thus filling the volume among the fuel pins [46]. These blocks are not bonded to the pressure tubes, therefore the heat transfer happens through the minimum gas gap in between them. In the event of cracking, chunks of the multiplier may be detached and have insufficient cooling, which may lead to unpredictable temperatures in the fragmented parts. Placing the Be_{12}Ti or any other neutron multiplier also inside the fuel pins would create a modular pin design, where the pin modules are produced in series separately in a controlled area [47].

Moreover, the manufacturing of the pin concept is complex. Welding each pressure tube to the FW requires methods that are currently not mature and, even when matured, would require joining $\approx 80,000$ pressure tubes to the FW, representing a challenge in terms of time, cost, and risks. The fabrication of an FW as a single part (or in a very limited number of parts, e.g., 2 or 3) is still deemed to be very challenging, and its feasibility for a DEMO size is still to be demonstrated. Finally, the assembly sequence of the HCPB BB may be problematic due to the exposed presence of Be_{12}Ti blocks [48].

These are the main reasons that led to the introduction of the alternative HCPB BB design based on an innovative pin monoblock architecture.

1.4 The HCPB monoblock Breeding Blanket

The HCPB monoblock design is characterised by design choices aiming at surmounting the critical issues affecting the reference HCPB BB. It is based on an innovative pin monoblock architecture (Figure 1.14), and it is characterized by the use of modular fuel pins containing all the functional materials, and by the absence of a typical FW element/component, as the plasma-facing surface is itself the apical area of the pins, where a tungsten armour is applied.

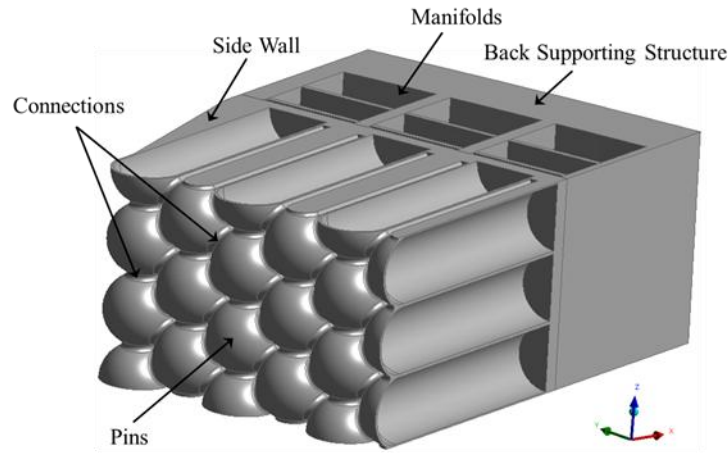


Figure 1.14: 3D representation of the steel domains of the innovative pin monoblock architecture [47].

As a result, in this alternative HCPB BB concept, the new arrangement of breeder and multiplier avoids the occurrence of unpredictable temperature hotspots in neutron multiplier fragments, as the damaged block is cooled anyway, being located within the pin (namely within the actively cooled domain). Moreover, the absence of a separate FW element strongly simplifies BB manufacturing and mitigates the problem of the low BB system reliability under DEMO conditions due to weld failure, as it results in a design where welds are only envisaged at the back of the BB. In the following, details are provided on the baseline layout of a fuel-breeder pin of the HCPB monoblock BB.

1.4.1 Baseline HCPB monoblock pin geometry

In the baseline HCPB monoblock pin layout (Figure 1.15) the breeder and the multiplier – constituted by ACB pebble beds ($\text{Li}_4\text{SiO}_4 + 35\%\text{mol Li}_2\text{TiO}_3$) and Be_{12}Ti blocks respectively – are arranged within a steel (ferritic-martensitic 9Cr-1WVTa steel, so called EUROFER97 and hereon referred to as “Eurofer” for the sake of readability) cladding, located in the actively cooled domain. The cooling helium enters the pin from the inlet manifold located in the back supporting structure, moves towards the Eurofer cap and spreads radially before moving backwards between the external Eurofer shell and the Eurofer outer sheet. Once the cooling helium reaches the Eurofer plug, it reverses its direction and flows between the Eurofer inner sheet and the Eurofer cladding, thus cooling the Breeding Zone (BZ) until it exits the pin domain to be collected in the outlet manifold. It is worth noting that, due to the flow-motion reversal, in the baseline pin layout the coolant performs a double pass (back and forth) through the pin domain. A 2 mm-thick tungsten coating is applied externally to the Eurofer cap, serving as the plasma-facing material. Moreover, a thin layer of stagnant helium is interposed between the two Eurofer sheets in

order to thermally decouple the cap – significantly affected by the heat flux acting on the tungsten plasma facing surface – from the BZ.

The stagnant helium layer and the flowing (cooling) helium form a continuous domain, as there is no physical separation between them. Specifically, the helium gap between the two Eurofer sheets is in direct communication with the main coolant flow in the zone of flow motion reversal, where no axial sealing is present between the sheets. On the opposite side of the gap, either an axial closure or an outlet leading to a confined region of stagnant helium located outside the pin domain is envisaged. Both solutions ensure that the helium between the two Eurofer sheets remains effectively stagnant.

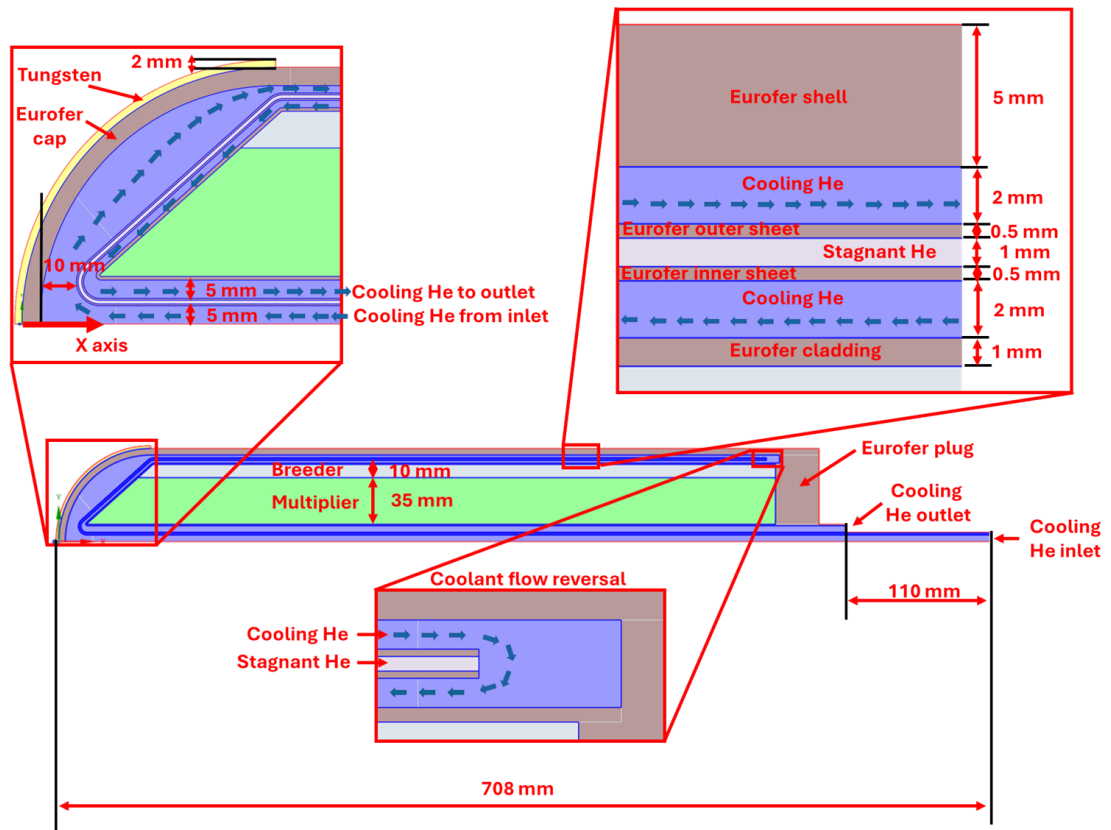


Figure 1.15: 2D CAD model of the baseline HCPB monoblock pin layout (symmetric about the X-axis). Blue arrows illustrate coolant flow.

Chapter 2 Constraints, objectives, and parameters of the HCPB monoblock pin layout optimization

2.1 Introduction

The first part of the work carried out during the PhD years focused on the assessment and optimization of the thermofluid-dynamics of a pin of the HCPB monoblock, with the purpose to achieve a pin geometric configuration characterized by acceptable structural and functional material temperatures, with an acceptably low coolant pressure drop. In this context, the sensitivity of the pin performance to different parameters has been examined under multiple working conditions.

In this phase, only the thermal and fluid-dynamic behaviour of the HCPB pin has been considered, while the thermomechanical behaviour of the most promising layouts has been assessed in a subsequent part of the activities. The present chapter details the HCPB monoblock design constraints and objectives, as well as the analysed parameters, geometric features and working conditions of the HCPB monoblock pin.

2.2 Design constraints and objectives for HCPB monoblock BB

The design constraints and objectives arise from the properties of the structural and functional materials characterizing the HCPB monoblock BB, as well as from the coolant and the requirements of the DEMO reactor (e.g., TBR, output power). In fact, it is necessary to ensure that each structural and functional material has a thermal field compatible with the respective ranges of working temperatures. Moreover, the coolant working temperatures and pressures affect the required pumping power, the efficiency of the thermal-to-electric energy conversion, and the thermal conductivity of the coolant. In the following, details are provided on the target working conditions of the solid and fluid domains of the HCPB monoblock.

2.2.1 Structural and functional materials

The ranges of working temperatures for the HCPB BB are hereby illustrated [49].

Table 2.1 shows the target operating ranges for the tungsten armour, Eurofer, ACB pebbles and beryllide. As for the tungsten armour – constituting the plasma-facing part of the HCPB monoblock – a higher upper temperature limit leads to an increased amount of local armour surface melting occurring in certain transient off-normal and accidental events. However, it is still to be determined to what extent surface melting can be tolerated in such scenarios [49], therefore the value indicated in Table 2.1 may be updated upon availability of further design specifications. As for Eurofer – the HCPB monoblock structural material – the target operating range has to be maintained in order not to compromise its structural integrity. The minimum temperature of the range is a provisional value defined considering the material’s ductile behaviour (ductile-brittle transition temperature) as function of the irradiation conditions and temperature evolution [49]. The maximum temperature, instead, is due to the fact that the Eurofer creep stress limit S_t drastically decreases beyond that limit [49]. Deviations from these values, hence, can be allowed if the structural integrity is not compromised, as in the case of Eurofer parts that do not have a structural function. For as regards the ACB pebbles constituting the breeder of the HCPB monoblock, the upper limit is defined in order to avoid the material degradation at higher temperatures under neutron irradiation, and for safety considerations (the high temperature minimizes the tritium inventory blocked in the breeder and thus maximizes the release) [49]. Finally, concerning the beryllide constituting the neutron multiplier of the HCPB monoblock, in this case the upper limit is defined in order to bound swelling, the material degradation at higher temperatures under neutron irradiation, and for safety considerations [49].

Table 2.1: Operating temperatures for tungsten armour, Eurofer, ACB pebbles, and Beryllide [49].

Material	Min. Temperature [°C]	Max. Temperature [°C]
Tungsten armour	—	1000±50
Eurofer	350±50	500±50
ACB pebbles	—	920±50
Beryllide	—	1000±50

As for the BZ – constituted by the ACB pebbles and the beryllide – it is to be underlined that solely ensuring a thermal field within the operating ranges cannot be deemed satisfactory. In fact, this part of the pin is demanded to produce tritium. The amount of tritium produced for a given configuration of the BZ (and of the rest of the structure that is not transparent to neutrons) can be assessed through dedicated neutronic analyses (e.g., Monte Carlo method-based analyses conducted with the code MCNP [50] [51]). In the

present work, no dedicated neutronic analysis has been performed on any HCPB monoblock pin layout, but the nuclear behaviour of the pin has been assumed to be that of an all-in-pin HCPB concept [52] with a BZ arrangement similar to that of the HCPB monoblock baseline pin layout (Figure 1.15), and that has been assessed through dedicated neutronic analyses showing a satisfactory tritium production. Details on the resulting nuclear heating (NH) load applied to the HCPB monoblock pin can be found in section 3.3, however it is hereby mentioned that the tritium breeding performance of the HCPB pin monoblock has been assumed to be satisfactory for the baseline layout (in accordance to [52]), and can be reasonably assumed to be improved in any configuration envisaging a wider BZ without any increase of the Eurofer thickness, and to be worsened in arrangements with either narrower BZ or higher Eurofer thickness, or both.

2.2.2 Coolant

For the cooling helium, the exercise pressure range (Table 2.2) is inherited from the BB-PHTS interface requirements [53]. In detail, the high pressure is derived from the need to reduce circulating power and minimize the size of pipework. The range is dictated by engineering judgement, where the low pressure is mostly driven by a need to reduce the flow speed in the pipework [54]. The design pressure is equal to the reference pressure multiplied by 1.15 and is used to perform conservative evaluations on the thermomechanical performance of components withstanding the helium pressure.

Table 2.2: Helium pressure range in HCPB BB [53] [54].

Material	Min. Pressure [MPa]	Reference Pressure [MPa]	Design Pressure [MPa]	Max. Pressure [MPa]
Helium	6	8	9.2	10

However, to achieve a satisfactory pin design, it is not sufficient to ensure that the helium pressure lies within the exercise range. In fact, the coolant pressure drop occurring within the pin domain has a significant influence on the required pumping power, which needs to be limited both for overall plant efficiency, and to comply with the limits imposed by the off-the-shelf technology available for the 16 helium blowers of the PHTS. In detail, in a helium-cooled blanket system, too large pressure drop inducing large circulating power could be a critical issue for the blanket system, since the state-of-the-art of the associated helium turbomachinery is limited to 6 MW per unit [55] [56] [57].

A design limit for the total pressure drop within a pin of the HCPB monoblock has not been defined yet, and its calculation is a complex task that needs to be performed through dedicated studies. However, basing on the calculations performed for the *fuel-breeder pin*

HCPB [58], it seems reasonable to assume that values below 0.5 bar are both acceptable and desirable, while higher values up to 0.8 bar may be retained high but still viable.

The helium exercise temperature range is shown in Table 2.3. The coolant temperatures are more a consequence of the temperature limitations of the structural steel more than a target by themselves [53], therefore they would be updated if a different structural material is employed, or an improved version of Eurofer is achieved.

Table 2.3: Cooling helium operating temperatures.

Material	Min. Temperature [°C]	Max. Temperature [°C]
Helium	325±25	550±50

Finally, helium velocity in the BB shall not exceed 80-100 m/s in order to limit vibration in the feeding pipes [54].

2.3 Investigated parameters and geometric features

In the HCPB monoblock pin design optimization, the performance sensitivity with respect to the variation of certain parameters has been evaluated. In detail, both geometrical parameters and the roughness value on machinable coolant-wetted surfaces have been taken into account. An overview of the investigated geometric features – discussed in more detail in the following – is hereby provided. For the variants envisaging a double coolant flow pass within the pin geometry (alike the baseline), the investigated parameters – shown in Figure 2.1 – are the thickness of the outlet channel t_1 , the jet impingement shape, the radius of the inlet tube r_1 , and the thickness of the outer annular channel t_2 . On the other hand, the studies on single flow pass layouts (unlike the baseline) examined the radius of the inlet tube r_1 , and the thickness of the outer annular channel t_2 , as shown in Figure 2.2. Regarding the jet-impingement shape, the conclusions drawn from the investigation of the double flow-pass layouts have been considered valid for the single pass configurations as well.

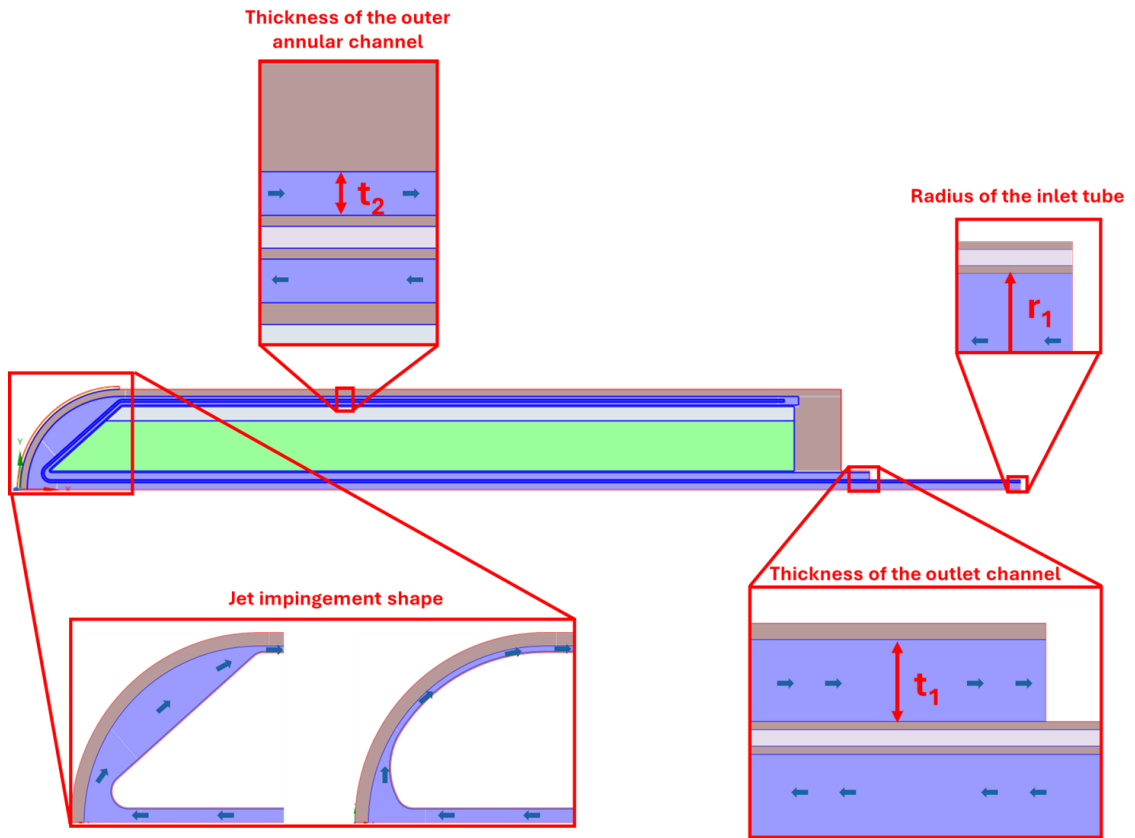


Figure 2.1: Investigated geometric features in double coolant flow pass layouts.

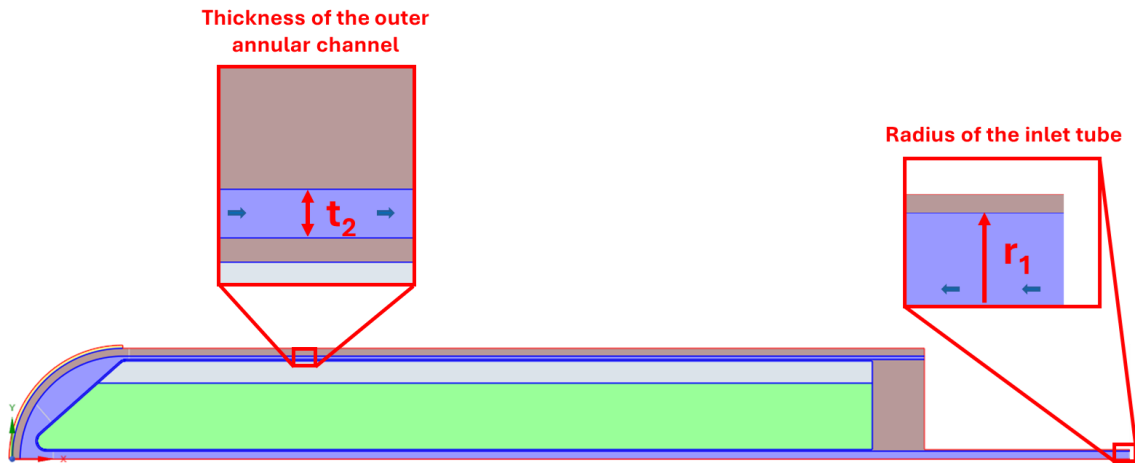


Figure 2.2: Investigated geometric features in single coolant flow pass layouts.

2.3.1 Thickness of the outlet channel t_1

In the baseline pin layout, the thickness of the annular outlet channel is of 5 mm (see parameter t_1 in Figure 2.3). Evaluations have been performed considering designs characterised by different t_1 values, as shown in Table 2.4.

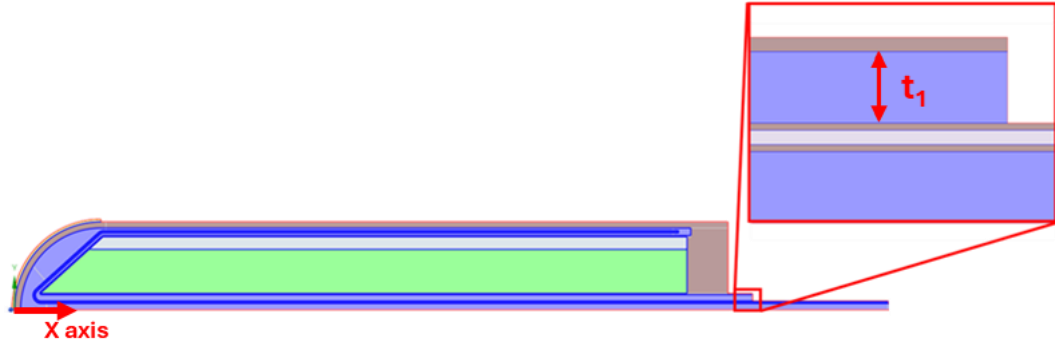


Figure 2.3: Outlet channel thickness t_1 .

Table 2.4: Analysed cases with different t_1 values.

t_1 [mm]	5	4	3	2	1
Name of the layout	case 1 (baseline)	case 7	case 6	case 2	case 8

It is to be specified that, in the baseline layout, it is possible to reduce the outlet channel thickness t_1 both by enlarging the stagnant helium thickness and by enlarging the BZ. The former case results – for given t_1 value – in a slighter reduction of the outlet channel cross section compared to the latter, as it implies that the helium flows farther from X symmetry axis. These two different possibilities have been examined comparing the performances of two layouts in which the same t_1 of 2 mm is realized in the two different ways, namely the case 2 and the case 3 layouts (see Figure 2.4). In detail, in the case 2 layout, the reduction of t_1 (compared to the baseline) is performed by enlarging the stagnant helium channel, and in the case 3 geometry, it is performed by enlarging the BZ.

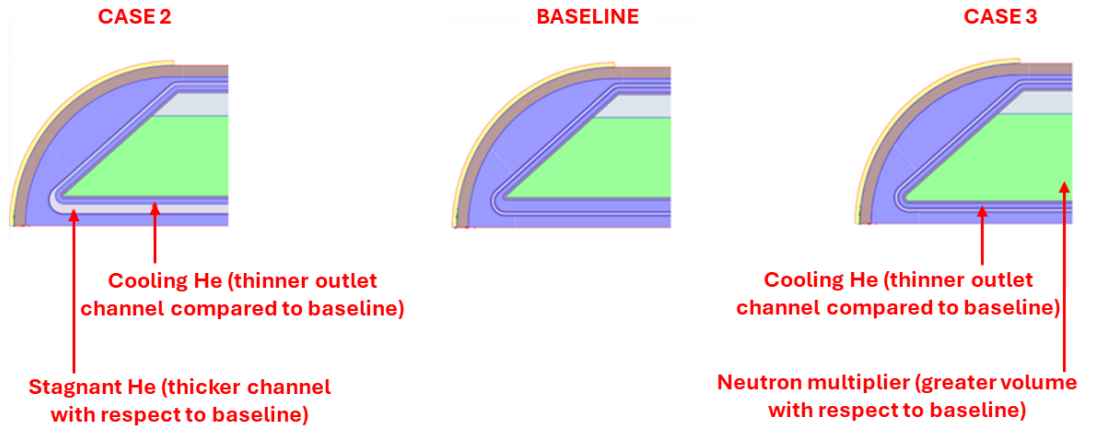


Figure 2.4: Apical area of baseline (case 1) geometry (middle), case 2 and case 3 (sides).

As will be detailed in section 3.5.2, case 2 layout presents better performances than the case 3 from the thermal and fluid-dynamic standpoints. For this reason, the reduction of t_1 in all the geometries shown in Table 2.4 is performed by enlarging the stagnant helium channel.

2.3.2 Jet impingement shape

The shape of the jet impingement region of the baseline layout has been based on the results obtained in former CFD studies [44] [59] regarding both the reversed funnel configuration and the 10 mm distance shown in Figure 2.5. However, another possible jet impingement shape has been tested. As detailed in 3.5.5, the thermal and fluid dynamic performances of the case 2 layout have been compared to those of the case 4 geometry. The latter is obtained starting from the case 2 layout, by modifying the geometry in the cap region (namely the pin front region) as shown in Figure 2.6. Here, the clearance for the cooling helium flow in the proximity of the Eurofer cap is 2 mm. This modification was aimed at investigating whether a more controlled helium flow in the cap region would enhance the heat transfer in this critical region.

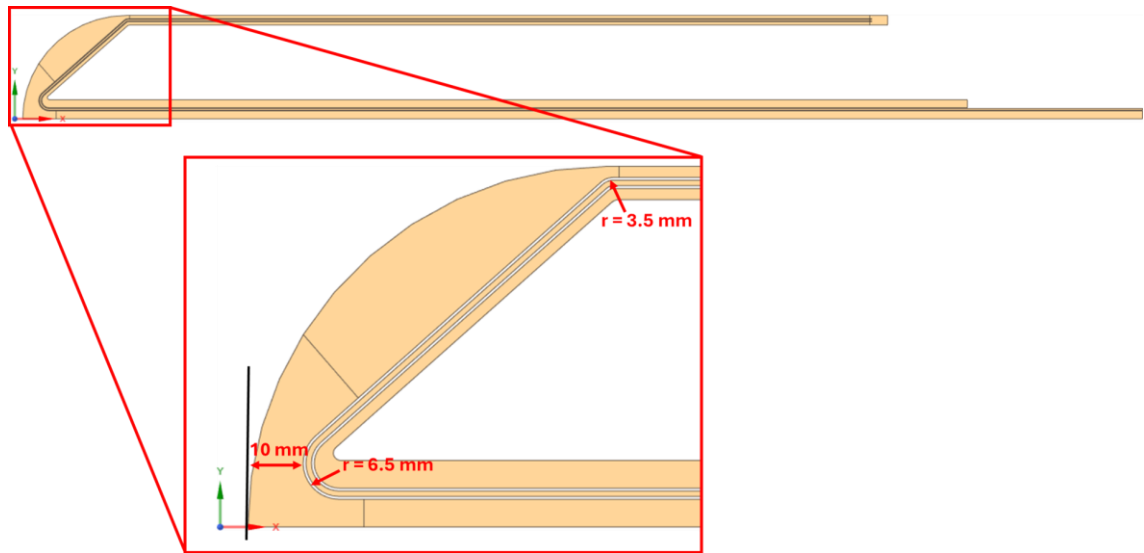


Figure 2.5: Helium domains of the case 1 (baseline) pin layout, with detailed view on the apical area.

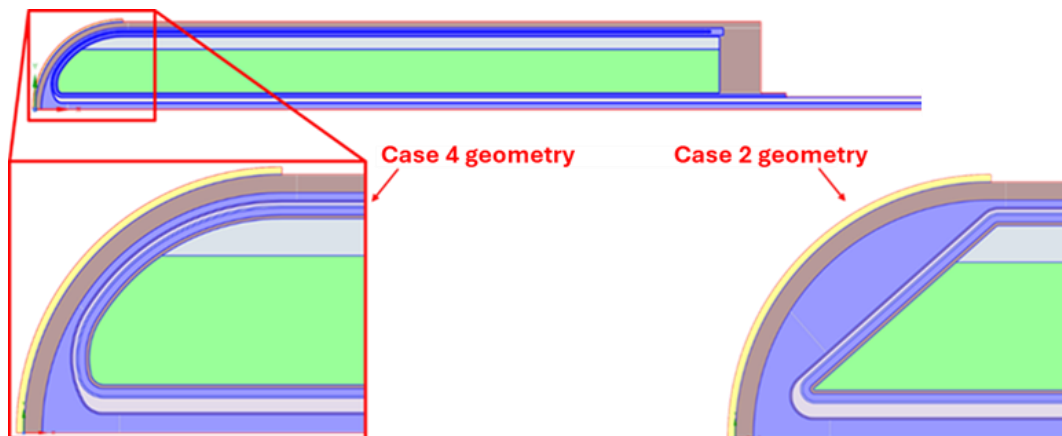


Figure 2.6: Case 4 geometry and comparison with case 2 layout.

2.3.3 Double and single helium flow pass

In the parametric studies, it has been evaluated the possibility of realizing pin layouts rather different from the baseline. In the baseline layout and on its derived variants, the coolant performs a double pass (back and forth) in the pin domain before exiting through the outlet annular channel. However, in light of the performances of such layouts (detailed in section 3.5), it appeared interesting to investigate pin layouts foreseeing a single pass of the coolant within the pin domain.

This idea gave birth to the case 5 pin layout, that is obtained starting from the baseline layout by removing the metal sheets and the stagnant helium, and enlarging the BZ by occupying the free space. For the sake of clarity, both the case 1 (baseline) and the case 5 geometry are shown in Figure 2.7.

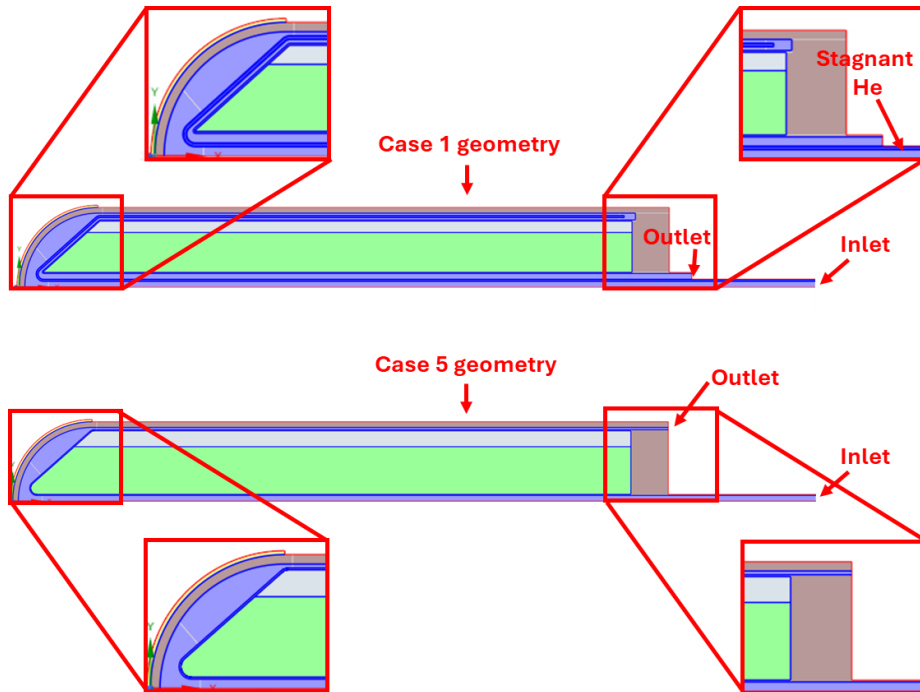


Figure 2.7: Case 5 geometry and comparison with case 1 layout.

2.3.4 Radius of the inlet tube r_1

In the baseline pin layout, the radius of the inlet tube is of 5 mm (see parameter r_1 in Figure 2.8).

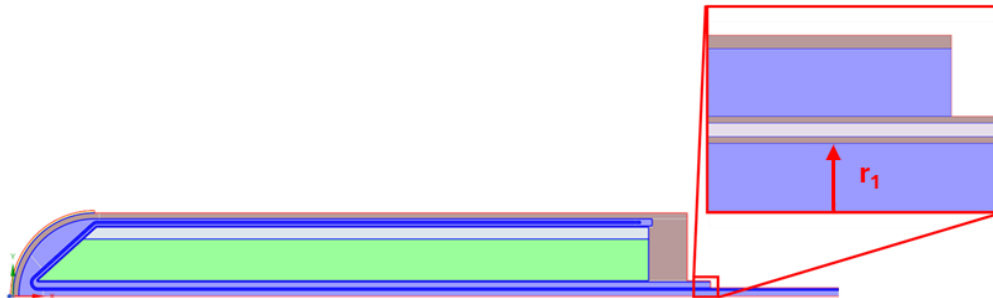


Figure 2.8: Inlet tube radius r_1 , shown in baseline pin geometry.

In the parametric studies, different combinations of the parameters t_1 (Figure 2.3) and r_1 have been analysed, according to the geometries illustrated in Table 2.5.

Table 2.5: Analysed combinations of parameters t_1 and r_1 .

t_1 [mm]	2	2	2	2
r_1 [mm]	5	6	7	8
Name of the layout	Case 2	case 9	case 11	case 12

Considering the dimensions of the baseline layout (Figure 1.15) and the parameters shown in Table 2.5, it is possible to notice that a geometric configuration characterized by the combination ($t_1 = 2$ mm , $r_1 = 9$ mm) cannot exist as it would leave no space for the layer of stagnant helium interposed between the inlet tube and the outlet channel.

Alike the baseline pin layout, also the case 5 (single helium flow pass) pin layout has a radius of the inlet tube of 5 mm (see Figure 2.9).



Figure 2.9: Inlet tube radius r_1 , shown in case 5 pin geometry.

Also in the case of single flow pass geometries, different values of the inlet tube radius r_1 have been considered, generating the configurations illustrated in Table 2.6.

Table 2.6: Analysed cases with different r_1 values and a single helium flow pass.

r_1 [mm]	4	5	6	7
Name of the layout	Case 10	case 5	case 13	case 14

2.3.5 Thickness of the outer annular channel t_2

In the baseline pin layout, the thickness of the outer annular channel is of 2 mm (see parameter t_2 in (Figure 2.10)). Alike the baseline pin layout, also the case 5 (single helium flow pass) pin layout has an outer annular channel thickness t_2 of 2 mm (Figure 2.11).

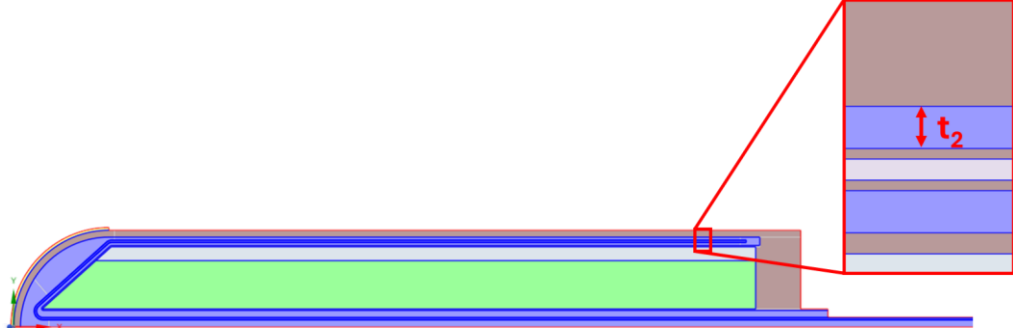


Figure 2.10: Outer annular channel thickness t_2 , shown in baseline pin geometry.



Figure 2.11: Outer annular channel thickness t_2 , shown in case 5 layout.

In the parametric analysis, considerations have been made on the influence of the outer annular channel thickness t_2 on the performances of double and single coolant flow pass pin architectures. The dissertation is presented in section 3.5.8, along with the results of a comparative analysis between two single flow pass geometries, namely the case 13 and the case 15 geometry, that is obtained from the case 13 layout by reducing the outer channel thickness from 2 mm to 1 mm (Table 2.7).

Table 2.7: Analysed combinations of r_1 and t_2 .

Coolant flow pass	single	single
t_2 [mm]	2	1
r_1 [mm]	6	6
Name of the layout	case 13	case 15

2.3.6 Roughness of coolant-wetted surfaces

Certain coolant-wetted regions of the HCPB monoblock baseline pin layout supposedly present the manufacturing feasibility of surface finishing. A local increase in surface roughness locally enhances the solid-fluid heat transfer at the cost of worsened behaviour in terms of pressure drop. In a first phase of the parametric studies, the values of surface

roughness shown in Table 2.8 have been assigned to the machinable surfaces, while a constant value of 20 μm has been kept elsewhere.

Table 2.8: Roughness values applied on machinable surfaces in parametric analyses.

Roughness [μm]	20	40	60	80	100	200
--------------------------------	----	----	----	----	-----	-----

In a second phase of the parametric analysis, attention has been focused on high levels of surface roughness, therefore these simulations considered the values shown in Table 2.9.

Table 2.9: Roughness applied on machinable surfaces in high roughness analyses.

Roughness [μm]	100	200
--------------------------------	-----	-----

In the pin layouts envisaging a double helium flow pass, the surfaces that supposedly present the manufacturing feasibility of surface finishing are shown in red (surfaces “a”), yellow (surfaces “b”), and green (surfaces “c”) in Figure 2.12. In detail, it is expected that the machining would be easier on surfaces “a” and “c” than in surfaces “b”.

On the other hand, in the geometries envisaging a single flow pass, the feasibility of surface machining has been assumed on all the coolant-wetted surfaces (Figure 2.13).

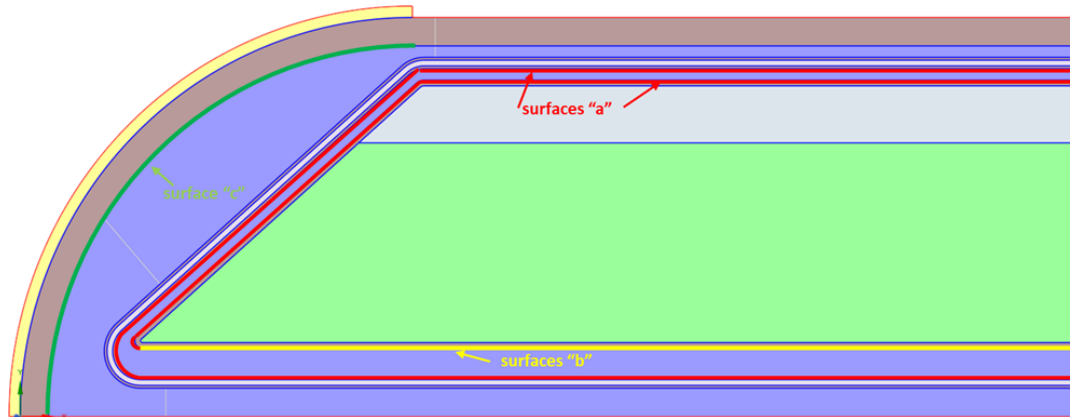


Figure 2.12: Illustration of the surfaces where feasibility of machining has been assumed (pin layouts with double helium flow pass).

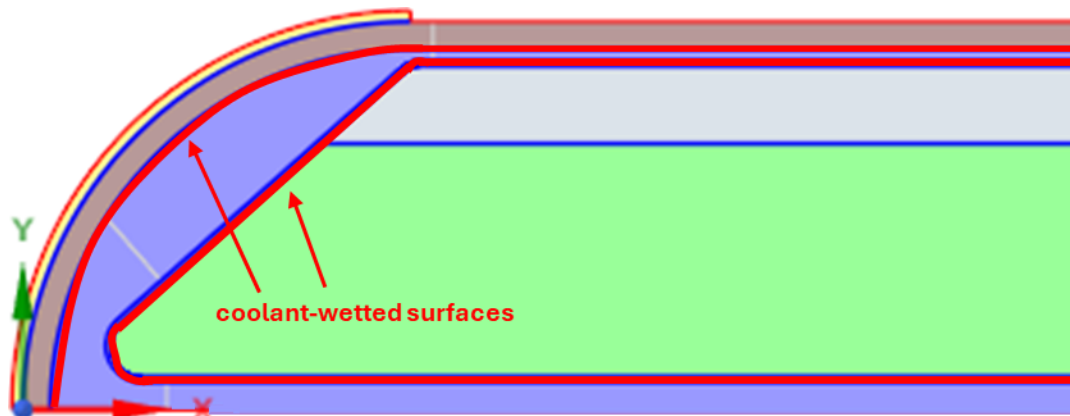


Figure 2.13: Illustration of the coolant-wetted surfaces of single flow pass layouts.

2.4 Examined working conditions

In the HCPB monoblock pin design optimization, the performance sensitivity with respect to the parameters detailed in 2.3 has been evaluated under different working scenarios in terms of coolant temperature rise and first wall heat flux. Conversely, the nuclear heating distribution has been kept fixed (see section 3.3.3).

In the following, details are provided on the considered operating conditions.

2.4.1 Coolant temperature rise ΔT

Increasing the coolant temperature rise in steady-state conditions allows to work with lower coolant mass flow rates (MFRs) and total pressure drops, thus reducing the required pumping power. Furthermore, higher coolant outlet temperatures result in a more efficient thermal-to-electric energy conversion. Nevertheless, increasing the coolant temperature rise, higher temperatures in the solid domains are expected, possibly jeopardizing the structural material integrity. In facts, it is hereby recalled that, as mentioned in section 2.2, the coolant operating temperature range is driven by that of the employed structural steel (Eurofer in the case of HCPB monoblock BB).

As a consequence, keeping the coolant inlet temperature fixed at 300 °C, the helium outlet temperature has been changed considering the different values of coolant temperature rise (ΔT) shown in Table 2.10.

Table 2.10: Coolant temperature rise values examined in parametric analyses.

ΔT [°C]	220	240	260	280	300
-----------------	-----	-----	-----	-----	-----

2.4.2 First wall Heat Flux

The thermal effects of the electromagnetic radiation emitted by plasma have been modelled as a surface heat load acting on the tungsten hemispherical plasma-facing surface. In detail, the imposed heat flux (HF) – in order to take into account the geometric configuration of the HCPB monoblock BB – has a maximum value HF_0 at the tip ($r=0$), and decreases down to 0 W/m² at the side ($r=R$), according to eq. (2.1):

$$HF = HF_0 \cdot \cos\left(\sin^{-1}\left(\frac{r}{R}\right)\right); \quad (2.1)$$

With $R=72$ mm in all the investigated cases.

The intensity of the electromagnetic radiation acting on the pin's plasma facing surface can vary based on the pin poloidal position within the BB and on the plasma operating scenario. In order to consider different pin working conditions, the different HF_0 values shown in Table 2.11 have been imposed in the parametric analyses.

Table 2.11: HF_0 values examined in parametric analyses.

HF_0 [MW/m ²]	0.3	0.48	0.65	0.83	1
-----------------------------	-----	------	------	------	---

Among the quantities shown in Table 2.11, 0.3 MW/m² represents the reference value for a plausible HCPB monoblock pin working condition, and the investigation on higher values represents a scoping analysis aimed at studying the cooling capabilities of the concept.

Chapter 3

Thermofluid-dynamic analysis of a pin of the HCPB monoblock BB

3.1 Introduction

The first part of the work carried out during the PhD years focused on the assessment and optimization of the thermofluid-dynamics of a pin of the HCPB monoblock, with the purpose to achieve a pin geometric configuration characterized by acceptable structural and functional material temperatures, with an acceptably low coolant pressure drop. In Chapter 2, details have been provided on the HCPB monoblock design constraints and objectives, as well as on the parameters, geometric features and working conditions that have been considered in the thermal and fluid dynamic optimization. The thermofluid-dynamic analysis campaign has been conducted following a theoretical-numerical approach based on the Finite Volume Method (FVM) and adopting the Ansys Fluent 2023 R2 [18] commercial CFD code. The axisymmetry of the pin domain has been exploited in order to set up 2D CFD models characterised by a significantly lower computational burden compared to 3D models, enabling the possibility to run a huge number of fast-running simulations.

In the following, the methodology, setup, and results of the thermofluid dynamic optimization are described.

3.2 Description of the methodology

The macroscopic behaviour of a fluid is governed by three conservation laws, namely mass, momentum, and energy conservation. Under the assumption that fluids can be treated as deformable continua, these laws of Nature take the form of the Navier-Stokes equations, which consist of a set of nonlinear Partial Differential Equations (PDEs). The system involves five PDEs, many closure and constitutive equations, and twenty variables, namely one for density, three for velocity components, one for pressure, nine for the viscous stress tensor, three for convective acceleration components, one for internal energy, and two for temperature and thermal conductivity.

In detail, 3.1 expresses the mass conservation law, 3.2 the momentum conservation, and 3.3 the energy conservation.

$$\frac{\partial \rho}{\partial t} + \nabla(\rho \vec{v}) = 0 \quad (3.1)$$

$$\frac{\partial(\rho \vec{v})}{\partial t} + \nabla(\rho \vec{v} \vec{v}) = -\nabla(p) + \nabla(\bar{\tau}) + \rho \vec{g} + \vec{F} \quad (3.2)$$

$$\frac{\partial(\rho c_p T)}{\partial t} - \nabla(\rho \vec{v} c_p T) = \nabla(\lambda \nabla T) + q'' \quad (3.3)$$

where p is the static pressure, $\bar{\tau}$ is the stress tensor, and $\rho \vec{g}$ and $\vec{F}$ are the gravitational body force and external body forces. The stress tensor $\bar{\tau}$ is given by:

$$\bar{\tau} = \mu \left[(\nabla \vec{v} + \nabla \vec{v}^T) - \frac{2}{3} \nabla \vec{v} I \right] \quad (3.4)$$

where μ is the molecular viscosity, I is the unit tensor, and the second term on the right-hand side is the effect of volume dilation.

The balance between the number of equations and the number of unknowns is reached with the definition of the fluid properties. A single solution of the equation can be obtained if Boundary Conditions (BCs) and initial conditions (along with the state equation) are given.

The analyses described in the present chapter involved both fluid and solid domains. The solid-related part of the study focuses on the heat transfer phenomena involving the solid domain. The unknown of this problem is the temperature field on the solid domain, and its governing equation is the heat balance equation:

$$\rho c_p \frac{\partial T}{\partial t} = \lambda \nabla^2 T + q''' \quad (3.5)$$

The axisymmetric nature of geometry, loads, and BCs in the case of the thermofluid-dynamic assessment of the HCPB monoblock pin allows to transform equations 3.1 and 3.2 in the set (3.6 – 3.11).

Mass conservation equation in 2D axisymmetric configuration:

$$\frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x}(\rho v_x) + \frac{\partial}{\partial r}(\rho v_r) + \frac{\rho v_r}{r} = 0 \quad (3.6)$$

Axial momentum conservation equation in 2D axisymmetric configuration:

$$\frac{\partial}{\partial t}(\rho v_x) + \frac{1}{r} \frac{\partial}{\partial x}(r \rho v_x v_x) + \frac{1}{r} \frac{\partial}{\partial r}(r \rho v_r v_x) = RHS_x \quad (3.7)$$

With:

$$RHS_x = -\frac{\partial p}{\partial x} + \frac{1}{r} \frac{\partial}{\partial x} \left[r \mu \left(2 \frac{\partial v_x}{\partial x} - \frac{2}{3} (\nabla \vec{v}) \right) \right] + \frac{1}{r} \frac{\partial}{\partial r} \left[r \mu \left(\frac{\partial v_x}{\partial r} + \frac{\partial v_r}{\partial x} \right) \right] + F_x \quad (3.8)$$

Radial momentum conservation equation in 2D axisymmetric configuration:

$$\frac{\partial}{\partial t}(\rho v_r) + \frac{1}{r} \frac{\partial}{\partial x}(r \rho v_x v_r) + \frac{1}{r} \frac{\partial}{\partial r}(r \rho v_r v_r) = RHS_r \quad (3.9)$$

With:

$$RHS_r = -\frac{\partial p}{\partial r} + \frac{1}{r} \frac{\partial}{\partial x} \left[r \mu \left(\frac{\partial v_r}{\partial x} + \frac{\partial v_x}{\partial r} \right) \right] + \frac{1}{r} \frac{\partial}{\partial r} \left[r \mu \left(2 \frac{\partial v_r}{\partial r} - \frac{2}{3} (\nabla \vec{v}) \right) \right] - 2\mu \frac{v_r}{r^2} + \frac{2}{3} \frac{\mu}{r} (\nabla \vec{v}) + \rho \frac{v_z^2}{r} + F_r \quad (3.10)$$

Where:

$$\nabla \vec{v} = \frac{\partial v_x}{\partial x} + \frac{\partial v_r}{\partial r} + \frac{v_r}{r} \quad (3.11)$$

And v_z is the swirl velocity [18].

The PDEs introduced above can be solved analytically only in some cases characterised by especially simple combinations of geometry, loads, and BCs. In general cases, as well as in the analyses presented hereby, a solution has to be pursued by means of numerical approaches. The field of science that studies fluid-dynamic problems with numerical analysis is called CFD.

One of the most suitable numerical methods used for representing and evaluating PDEs is the FVM which, by discretizing the domain, permits to turn the PDEs that describe the behaviour of the system in a set of algebraic equations.

In the FVM, the computing domain is subdivided into non-overlapping small volume elements characterized by a barycentre node and contact surfaces between adjacent volumes. The conservation laws are then expressed in the integral form, discretized and solved in each volume element of the domain. Therefore, the higher is the number of the volumes, the heavier will be the computational load. The numerical solution – performed through the qualified Ansys Fluent CFD code, based on FVM – of the set of equations 3.6 – 3.11 has a significantly lower computational burden than that of equation 3.1, 3.2 and 3.3. The consequent reduction in the calculation times, allowed to analyse the thermofluid-dynamic

performance of a broad variety of HCPB monoblock pin layouts, allowing to assess the influence of several parameters under different loading conditions.

In more detail, to numerically study and optimise the steady-state thermofluid-dynamic behaviour of the HCPB monoblock pin, 2D steady-state CFD analyses have been run. To this purpose, specific 2D FV models have been setup for each and every combination of parameters and loading conditions that have been considered. However, the numerous models that have been developed and the analyses that have been conducted share several common features. The procedure for creating the 2D CFD models representing the HCPB monoblock pin layouts can be outlined in the following steps:

- drawing of the 2D geometry of the relevant pin layout: the solid and fluid domains to be analysed have been drawn from scratch adopting the Ansys SpaceClaim 3D CAD modelling software [60]. In this step, the geometric features described in section 2.3 are created. Specifically, the Ansys SpaceClaim tool allows to draw geometric topologies spanning from 0D (points) to 3D (solids). To generate a 2D model representative of the actual 3D pin layout, lines and surfaces have been properly drawn choosing the X axis as the pin symmetry axis (see Figure 1.15). These lines and surfaces, once revolved around the X axis, respectively generate the surfaces and volumes constituting the HCPB monoblock pin. It is to be noticed that the geometric entities created in this step are completely agnostic in terms of materials and symmetry conditions, and may in principle represent an actual 2D topology as well as the 2D representation of an infinite slab, or even the 2D section of a solid obtained by revolution around an axis that is different from the X axis. Therefore, the subsequent steps are crucial to make the model representative of the HCPB monoblock pin;
- discretization of solid and fluid domains: in this phase, the proper FV model is generated by dividing the 2D entities created in the first steps into elements that communicate through nodes and that constitute the calculation grid (also named mesh). The discretization has been performed through the Ansys Mesh Tool [61]. During this phase, a first distinction is made between solid and fluid domains, as different characteristics of the calculation grid are required to properly model the phenomena occurring in a flowing fluid rather than a solid. In fact, in the parts of the fluid domain located in the immediate vicinity of the solid-fluid interfaces, a thin layer of fluid develops - named boundary layer - in which high gradients of fluid-dynamic quantities are observed. In more detail, the portion of the boundary layer in contact with the wall observes a no-slip condition (zero relative velocity with respect

to the wall), and flow conditions rapidly vary moving away from the wall, equating to bulk flow conditions at the end of the boundary layer. Failures in detecting the boundary layer behaviour cause significant errors in the results of the analysis, and may also lead to numerical instabilities in the solution procedure. Therefore, significant mesh refinement is performed on the portions of the fluid domain that are in the vicinity of solid-fluid interfaces (Figure 3.1).

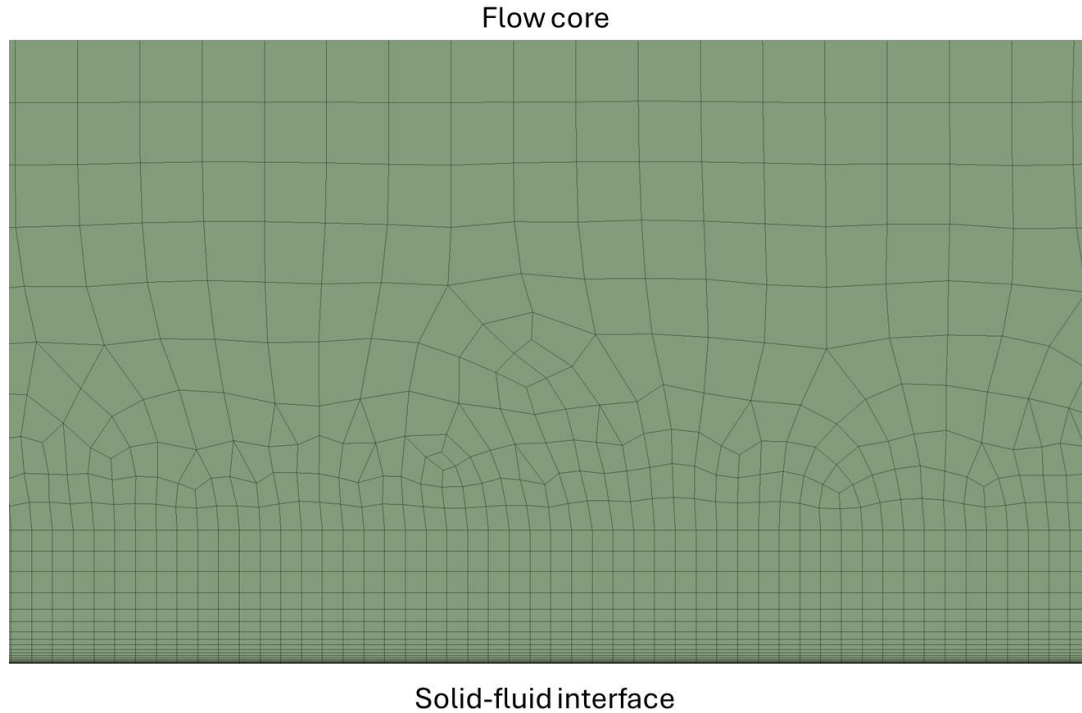


Figure 3.1: Transition from a coarser mesh in the core flow region to a more refined mesh at solid-fluid interface.

On the other hand, as to the discretization of solid domains, a minimum of three elements has been created across the thinnest regions, to ensure that heat diffusion phenomena are accurately captured. All the interfaces between different regions of the grid are characterized by conformal meshes. Finally, in this step, named selections have been created to aid the subsequent phases of the analysis setup;

- definition of constitutive model: the helium coolant has been modelled as an ideal gas with molecular weight taken from the IUPAC Periodic Table [62]. The thermofluid dynamic and transport properties of the coolant and the thermal conductivities of solid domains are the only relevant material properties to be defined in a steady-state thermofluid-dynamic problem, and they have been assigned coherently with former EUROfusion studies on HCPB [63]. The definition of these properties and their assignment to the relevant solid and fluid domains have been performed in Ansys Fluent [18];

- selection of loads and BCs: as for heat loads, the nuclear heating and the radiative heat flux have been considered. The first was drawn from dedicated neutronic studies on the similar SP9 all-in-pin HCPB configuration [52], while the second has been imposed according to the considered loading scenario as detailed in section 2.4.2. In this phase of the setup, the axisymmetry about the X axis is imposed as a BC, and thus the 2D model becomes effectively representative of the HCPB monoblock pin geometry. As to the helium coolant, for the outlet BC, the 8 MPa reference pressure (see 2.2.2) has been set as fluid total pressure. Moreover, as inlet BC the fluid temperature has been set to 300 °C and a specified MFR is imposed. In detail, the coolant MFR has been tuned so to ensure the desired coolant temperature rise (parametrised, see 2.4.1) for every loading scenario. On the coolant-wetted surfaces, a no-slip rough wall condition has been specified, with parametrised sand-grain roughness value (see 2.3.6). The aforementioned settings have been implemented in Ansys Fluent;
- definition of the turbulence model: the $k-\omega$ SST turbulent model was used (as in other CFD EUROfusion studies on HCPB [64]) because it is reliable in predicting flow separations arising from strong adverse pressure gradients, which is of key importance in the study of the flow field in the jet impingement region. It is a hybrid model that uses a conversion of the $k-\epsilon$ model into a $k-\omega$ model in the near-wall region, while adopts the $k-\epsilon$ model in the fully turbulent region far from the wall (Figure 3.2).

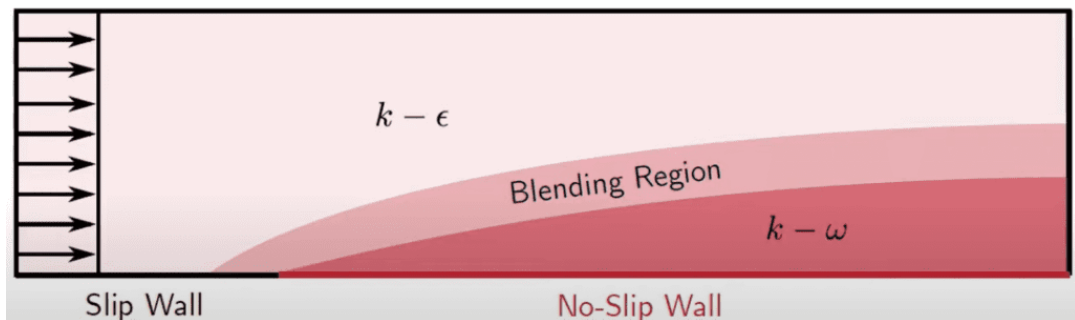


Figure 3.2: K- ω SST model switches between $k-\epsilon$ and $k-\omega$ turbulence model using a blending function [65].

Once the steps above are carried out, the simulation can be launched, and the code solves the Navier-Stokes equations through iterative calculations adopting suitable discretization and linearization schemes. The iterations can be stopped upon verification of user-defined convergence criteria, and, finally, it is possible to post-process the results.

3.3 2D CFD model setup

As illustrated in section 3.2, in order to run a thermofluid-dynamic CFD analysis based on the FVM method, it is necessary to perform the discretization of solid and fluid domains, and to define material properties, heat loads and boundary conditions. The present section provides quantitative details on these phases of the thermofluid-dynamic FVM modelling of the HCPB monoblock pin.

3.3.1 Calculation grid

Every 2D HCPB monoblock pin layout has been created from scratch by means of Ansys SpaceClaim and subsequently has been discretized using the Ansys Mesh Tool. The discretization of the solid domains has been performed by guaranteeing a minimum of three elements across the thickness of the thinnest regions to ensure that heat diffusion phenomena are accurately captured. As to the helium domains, in the layouts envisaging a layer of stagnant helium (namely those characterised by a double helium flow pass), the stagnant helium has been modelled – and hence discretized – as a solid domain: this strategy allows to avoid the useless computational effort of solving Navier Stokes equations on a domain constituted by stagnant fluid. For as regards the coolant, the mesh refinement in the proximity of the walls has been carried out by specifying the first layer thickness, the layers growth rate, and the number of inflation layers. The values of these quantities have been chosen by ensuring a discretization adequate to turbulence and roughness modelling according to the Ansys Fluent guidelines [18]. The size of the elements in the bulk region has been chosen based on the experience and selecting values that would allow to obtain grid-independent results with a low computational burden. The adopted settings have been validated through the grid independence analysis reported in section 3.4. Moreover, the bulk mesh has been refined in the coolant regions of the jet impingement and – for the layouts envisaging a double coolant flow passage – of fluid motion reversal (Figure 1.15). The described discretization settings are summarized in Table 3.1.

Table 3.1: Summary of mesh settings.

Mesh parameter	Value
Elements Topology	Hybrid (Quad/Tria)
Inflation Layer Number	20
First Layer Thickness [μm]	2
Layers Growth Rate	1.3
Element size [mm]	0.5/0.25 in refined zones

To provide an example, Figure 3.3 shows the mesh obtained for the baseline pin layout with the settings of Table 3.1, characterized by $6.6 \cdot 10^5$ elements connected in $6.6 \cdot 10^5$ nodes: observing the image in the red box, it is possible to appreciate the mesh refinements in the proximity of the coolant-wetted walls and the transition from the coarser to the finer bulk mesh in the jet impingement region.

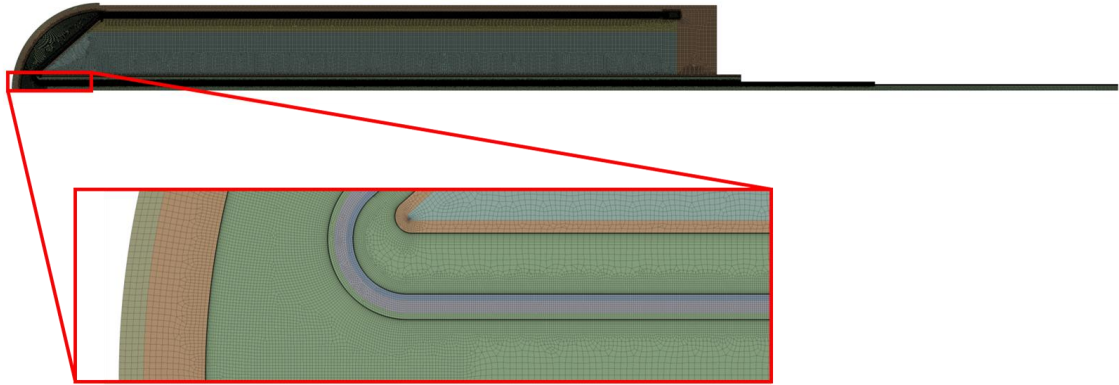


Figure 3.3: Mesh of the baseline pin layout according to the parameters of Table 3.1.

It is to be mentioned that the discretization settings of Table 3.1 have been employed in a first part of the thermofluid-dynamic analysis campaign (detailed in section 3.5), where the considered values of surface roughness on machinable coolant-wetted surfaces (see 2.3.6) only ranged from 20 μm to 100 μm . However, as it is widely described in section 3.5, the subsequent phase of the thermofluid-dynamic investigation focused on the pin behaviour with high values of roughness on machinable surfaces, namely 100 μm and 200 μm . To obtain reliable outcomes in such conditions, the roughness model implemented on Ansys Fluent has been updated from the Standard to the High Roughness (Icing) model. Consequently, attending the Ansys Fluent guidelines [18], also the features of the mesh refinement in the proximity of coolant-wetted walls have been changed for the analyses characterized by high surface roughness. In detail, the discretization settings adopted for high roughness analyses are shown in Table 3.2.

Table 3.2: Summary of mesh settings for high roughness analyses.

Mesh parameter	Value
Elements Topology	Hybrid (Quad/Tria)
Inflation Layer Number	29
First Layer Thickness [μm]	0.2
Layers Growth Rate	1.3
Element size [mm]	0.5/0.25 in refined zones

For the sake of completeness, the discretization adopted for the case 5 layout (see Figure 2.7) according to the settings of Table 3.2, characterized by $3.6 \cdot 10^5$ elements connected in $3.6 \cdot 10^5$ nodes is shown in Figure 3.4.

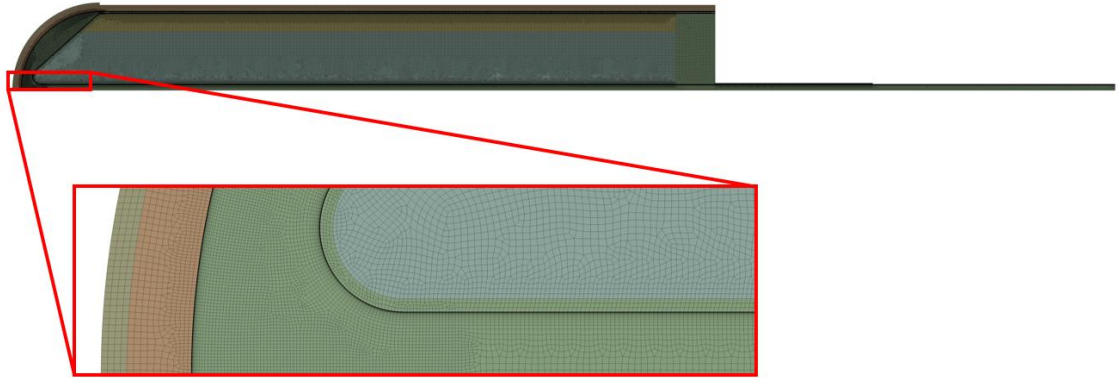


Figure 3.4: Mesh of the case 5 pin layout according to the parameters of Table 3.2.

The settings employed (either Table 3.1 or Table 3.2) and the resulting number of nodes and elements of each FVM model that has been developed for the thermofluid-dynamic campaigns are briefly reported in section 3.5, along with the description of the relevant analyses.

3.3.2 Material properties

With the scope to model properly the fluid and solid domains of the HCPB monoblock pin, material properties have been defined in Ansys Fluent considering the analysis practices and latest data available on the HCPB numerical modelling.

The helium coolant has been modelled as an ideal gas, with molecular weight taken from the IUPAC Periodic Table [62]:

$$MW_{He} = 4.0026 \left[\frac{kg}{kmol} \right] \quad (3.12)$$

The specific heat of helium gas has been assumed to be constant, and the value has been set in agreement with relevant studies on HCPB BB in the frame of EUROfusion [63]:

$$c_{p_{He}} = 5197 \left[\frac{J}{kg \cdot K} \right] \quad (3.13)$$

As to the thermal conductivity of cooling helium, a temperature-dependent expression has been employed (Eq. 3.14), obtained from [63] imposing a pressure of 80 bar:

$$k_{He}(T) = 2.92295 \cdot 10^{-3} \cdot T^{0.69864} \left[\frac{W}{m \cdot K} \right] \quad (3.14)$$

Where temperature is expressed in Kelvin.

Finally, the viscosity of the coolant was modelled through a power law [63]:

$$\mu_{He}(T) = 3.674 \cdot 10^{-7} T^{0.7} \left[\frac{kg}{m \cdot s} \right] \quad (3.15)$$

Where temperature is expressed in Kelvin.

As shown in section 2.3, the pin layouts characterized by a double helium flow pass (e. g. the baseline layout shown in Figure 1.15) envisage a layer of stagnant helium. In order to reduce the computational burden, stagnant helium has been modelled as a solid characterized by the same thermal conductivity as the cooling helium (see Eq. 3.14).

Since density and specific heat of solid domains do not affect the thermal field assessed in the performed steady-state analyses, uniform values have been assigned to these properties of solid materials. On the other hand, the thermal conductivities of solid domains do affect the thermal distribution, and their variations with temperature have been taken into account. As to ACB pebbles, beryllide blocks and Eurofer, piecewise linear functions of temperature have been defined according to the tables below, with values extracted from [63]:

Table 3.3: Thermal conductivity of ACB pebbles.

Temperature [°C]	Thermal conductivity [W/mK]
20	0.919
50	0.929
100	0.946
1000	1.249

Table 3.4: Thermal conductivity of beryllide blocks.

Temperature [°C]	Thermal conductivity [W/mK]
Irradiated up to 37.1 dpa (displacements per atom) at 650 °C	
350	37.2
450	44.3
600	43.5
650	45.4

Table 3.5: Thermal conductivity of Eurofer.

Temperature [°C]	Thermal conductivity [W/mK]
20	31.5
100	32.2
200	32.7

300	33.2
400	33.3
500	32.8
600	32.3
700	44.8

Finally, the thermal conductivity of tungsten has been modelled in Ansys Fluent via a polynomial function of temperature drawn from [63]:

$$k_W(T) = 174.9274 - 0.1067T + 5.0067 \cdot 10^{-5}T^2 - 7.8349 \cdot 10^{-9}T^3 \left[\frac{W}{m \cdot K} \right] \quad (3.16)$$

Where temperature is expressed in degrees Celsius.

3.3.3 Heat loads

Two types of heat loads have been considered: the nuclear heating and the radiative heat flux. The domain external surfaces not exposed to plasma radiation have been supposed to experience – in steady-state conditions – very limited heat exchange with the rest of the tokamak and therefore have been imposed to be adiabatic.

The NH, arising from the interactions of fusion neutrons and gammas with solid materials' nuclei, has been modelled as a volumetric heat source. In the present work, no dedicated neutronic analysis has been performed on any HCPB monoblock pin layout, but the nuclear behaviour of the pin has been assumed to be that of the similar SP9 all-in-pin HCPB concept [52], and that has been assessed through dedicated neutronic analysis. Therefore, the power density distributions relevant to the SP9 concept have been employed on solid domains except for tungsten, where a uniform value (Eq. 3.17) has been set in agreement with former studies on HCPB [64]:

$$NH_W = 25.8833 \left[\frac{W}{cm^3} \right] \quad (3.17)$$

In more detail, the volumetric heat loads relevant to the SP9 variant were available in the form of tables containing the NH values for different X coordinates; to ease the implementation on Ansys Fluent, these power density distributions have been mapped with decreasing exponential functions of the X coordinate fitted with the least square criterion. The expressions obtained for ACB pebbles, the beryllide block, and the Eurofer domains are shown in Eqs. 3.18, 3.19, and 3.20 respectively:

$$NH_{ACB} = 24.6065 \cdot e^{-4.5761 x} \left[\frac{W}{cm^3} \right] \quad (3.18)$$

Where x is expressed in [m].

$$NH_{Be_{12}Ti} = 11.2210 \cdot e^{-6.8776 x} \left[\frac{W}{cm^3} \right] \quad (3.19)$$

Where x is expressed in [m].

$$NH_{E97} = 9.9932 \cdot e^{-5.6972 x} \left[\frac{W}{cm^3} \right] \quad (3.20)$$

Where x is expressed in [m].

The resulting power density distributions on the baseline HCPB monoblock pin layout are shown in Figure 3.5.

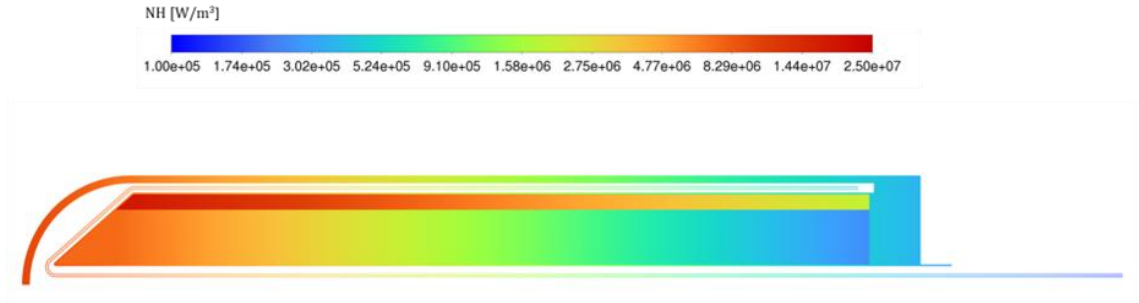


Figure 3.5: Nuclear Heating applied to breeder, multiplier, and Eurofer domains.

The second type of load that has been considered is the plasma surface load acting on the tungsten plasma-facing surface, and – as detailed in section 2.4.2 and hereby recalled – it has been modelled as a not-uniform surface heat load described by Eq. 3.21:

$$HF = HF_0 \cdot \cos \left(\sin^{-1} \left(\frac{r}{R} \right) \right) \quad (3.21)$$

With R=72 mm in all the investigated cases and HF_0 dependent on the considered loading scenario according to Table 2.11.

The total deposited power on a single pin is the sum of the power deposited by the volumetric heat load constituted by the NH, and the incident power due to the surface heat flux, according to Eq. 3.22:

$$\dot{Q}_{tot} = \dot{Q}_{vol} + \dot{Q}_{surf} \quad (3.22)$$

In the case of the HCPB monoblock, the volumetric deposited power is, in turn, the sum of the power deposited by NH in the different solid material domains (Eq. 3.23).

$$\dot{Q}_{vol} = \sum_j \dot{Q}_{vol_j} \quad (3.23)$$

Where each contribution $\dot{Q}_{vol_j}$ can be calculated by Eq. 3.24.

$$\dot{Q}_{vol_J} = \int_{V_J} NH_J dV_J \quad (3.24)$$

It is to be noticed that, for given pin layout, the volumetric deposited power calculated by Eq. 3.23 is constant as the terms NH_J (Eqs. 3.17 to 3.20) have been kept constant in all the considered parameter and load combinations of the thermofluid-dynamic analysis campaign. However, the volumetric deposited power can change significantly based on the considered pin layout, since different layouts imply different volumes of the solid materials domain and also different shapes of the volumes (the shape has an influence as, in general, NH_J is not uniform), which affect the computation of the contributions $\dot{Q}_{vol_J}$ via Eq. 3.24. The difference between the volumetric deposited powers in different pin geometries is noticeably high when comparing the layouts with a double helium flow pass with the geometries envisaging a single helium flow pass; in fact, the latter layouts have significantly larger breeder and multiplier volumes (due to the absence of the stagnant helium – that is transparent to neutrons – and of the Eurofer sheets). Moreover, it is reasonable to expect that these configurations allow a significantly higher tritium production, although dedicated neutronic analyses are required to confirm and quantify the extent.

As to the surface heat load, in any pin geometry the only heat flux to be considered is the plasma radiation incident on the tungsten hemispherical plasma-facing surface S_w . Therefore, in Eq. 3.22, the surface term is simply given by:

$$\dot{Q}_{surf} = \int_{S_w} HF dS_w \quad (3.25)$$

Where HF is given by Eq. (3.21). It can be noticed that the surface heat load changes based on the considered loading scenario as, in Eq. (3.21), HF_0 is dependent on the considered loading scenario according to Table 2.11. On the other hand, every pin layout has the same geometry for the tungsten domain, therefore, for each loading scenario, $\dot{Q}_{surf}$ and $\dot{Q}_{vol_w}$ are the same for all the pin layouts.

Ansys Fluent can calculate numerically the integrals of Eqs. 3.24 and 3.25: the imposed NH s and the radiative heat flux of Eq. (3.21) with an HF_0 value of 0.3 MW/m² result in a total deposited power $\dot{Q}_{tot}$ on the baseline pin geometry of 30.98 kW.

3.3.4 Boundary conditions

The CFD analysis campaign on the HCPB monoblock pin aimed at assessing and optimising the steady-state thermofluid-dynamic behaviour of the pin by means of fast-running 2D axisymmetric analyses that allow to explore the influence of several parameters under different loading conditions. Table 3.6 shows a summary of the setup of the CFD analyses.

Table 3.6: Summary of the thermofluid-dynamic analyses setup.

Analysis Setting	Value
Analysis Type	Steady-state
Domain Features	2D axisymmetric
Fluid Inlet Temperature	300 °C
Inlet BC	MFR (according to Eq. 3.28)
Outlet BC	8 MPa total pressure
Turbulence Model	k- ω SST
Wall BC	No-slip rough wall condition

The illustrated settings clearly reflect the nominal working conditions of the HCPB BB. However, it is important to provide some further information on the choice of the MFR that has been set as inlet BC in each analysis, as its value plays a critical role in exploring the different working conditions of the pin.

The rationale of the choice of the MFR is to tune the value so to ensure the desired coolant temperature rise for every considered loading scenario. In fact, the thermofluid dynamic campaign aimed to consider different values of the coolant temperature rise (see Table 2.10), as well as different intensities of the plasma radiation load (through the parameter HF_0 , see Table 2.11). The different values of the plasma radiative heat flux result in different total deposited powers on the pin, according to relations (3.22) and (3.25).

As can be seen in Table 3.6, the fluid inlet temperature is imposed as a BC. On the other hand, the fluid outlet temperature is an output of the analysis, and is related to the coolant temperature rise through equation (3.26):

$$\Delta T = T_{out} - T_{in} \quad (3.26)$$

In steady-state conditions, the total deposited power on the pin is entirely absorbed by the helium coolant. Since no phase change occurs in the helium gas, and since no work is done by the helium (as it does not interact with any moving part within the pin domain), all the

thermal power received by the coolant is sensible heat that translates in its temperature rise according to relation (3.27):

$$\dot{Q}_{tot} = MFR \cdot c_{p_{He}} \cdot \Delta T \quad (3.27)$$

Therefore, in each analysis, a value of HF_0 is examined (Table 2.11), resulting in a specific total deposited power $\dot{Q}_{tot}$. Moreover, each analysis is aimed at considering a specific target value of coolant temperature rise ΔT_{targ} (Table 2.10). To obtain this, the MFR is imposed as inlet BC according to relation (3.27), resulting in:

$$MFR = \frac{\dot{Q}_{tot}}{c_{p_{He}} \cdot \Delta T_{targ}} \quad (3.28)$$

An advantage of this approach is that, since the fluid outlet temperature is an output of the simulation, the resulting coolant temperature rise ΔT can be compared to the target value ΔT_{targ} providing valuable information on the convergence of the heat transfer calculations, as well as on the soundness of the analysis setup. All the analyses presented in section 3.5 are characterized by very low differences between ΔT and ΔT_{targ} , always below 1 °C.

It has been verified that the conditions of the fluid flow are always far from the compressible regime (Mach number lower than 0.3). In facts, when considering the narrowest flow section – the inlet tube of the case 10 geometry, with a radius of 4 mm – and the highest MFR imposed in the relevant analyses (under 1 MW/m² HF_0 and 220 °C coolant ΔT), conservatively assuming the helium density value relevant to the coolant outlet temperature (namely 4.7972 kg/m³), and considering, for simplicity, a flat velocity profile, the resulting helium velocity is roughly 173 m/s. On the other hand, under the coolant operating conditions, the minimum speed of sound in helium at 80 bar occurs at 300 °C and is equal to 1431 m/s. Hence, evaluating the highly conservative ratio between the two speeds would yield a Mach number of 0.12.

3.4 Grid independence evaluation

To ensure that the analysis setup presented in section 3.3 provides results that are not affected by inadequate domain discretization, grid independence analysis has been performed. In more detail, it has been verified that minimal result variations occur under significant changes in both the mesh element size and the refinement applied at the fluid-solid interfaces. To evaluate the stabilization of the solution, two metrics have been adopted: the total pressure drop and the fluid velocity profile in a section of the inlet tube where the flow is fully developed. Specifically, the total pressure drop has been selected by virtue of being a quantity of paramount importance in assessing the performance of any pin layout,

while the fluid velocity profile has been chosen to have a detailed and local (rather than global) indication of the flow field solution.

The study considered a peak radiation load HF_0 of 0.3 MW/m^2 , a coolant temperature rise ΔT of $220 \text{ }^\circ\text{C}$, and a roughness value of $20 \text{ }\mu\text{m}$ on all the coolant-wetted surfaces. It is also worth indicating that the analysis has been conducted on the baseline pin layout, and it validated the discretization settings of Table 3.1. However, due to the similarity in the coolant working conditions and flow paths characterizing the different layouts, the mesh independence evaluation has been retained valid also for the subsequently assessed pin geometries different from baseline. Moreover, as to the high roughness analyses adopting the discretization settings of Table 3.2 it is hereby pointed out that the modification of the first layer thickness performed to adapt to the Ansys Fluent Icing model has been accompanied by an increase in the number of layers that ensures to cover a slightly wider total thickness (namely 1.34 mm) compared to the settings of Table 3.1, while maintaining the same layers growth rate. Hence, the grid independence evaluations hereby presented have been retained valid also for the high roughness analyses, thus covering all the models adopted in the thermofluid-dynamic analyses of the present chapter.

Regarding the mesh element size, three analyses have been compared, labelled “M₁”, “M₂”, and “M₃”. In more detail, the analysis presented in section 3.5.1 has been selected as the reference (M₂), and two additional analyses have been performed by doubling (M₁) and halving (M₃) the mesh element size respectively. The percentage differences of the evaluated total pressure drops are shown in Table 3.7, and indicate that the element size adopted in the reference M₂ analysis endows a mesh-independent estimate of the total pressure drop.

Table 3.7: Total pressure drop sensitivity to mesh element size.

Analysis	Element size [mm]	Nodes/Elements	Percentage variation of evaluated total pressure drop
M ₁	1/0.5 in refined zones	$5.8 \cdot 10^5 / 5.8 \cdot 10^5$	– 0.14%
M ₂	0.5/0.25 in refined zones	$6.6 \cdot 10^5 / 6.6 \cdot 10^5$	Reference
M ₃	0.25/0.125 in refined zones	$11.9 \cdot 10^5 / 11.9 \cdot 10^5$	+ 0.013%

Figure 3.6 shows the fluid velocity profiles calculated in the three analyses M₁ to M₃ in a section of the inlet tube where the flow is fully developed. In the image, $r = 0$ corresponds to the axis of the inlet tube, while $r = r_1$ indicates the fluid-solid interface (where $r_1 = 5 \text{ mm}$ since the baseline pin layout is under evaluation). From a visual and qualitative inspection, the three velocity profiles appear to be rather similar. To gain quantitative indications, fluid velocity has been sampled at three different radial positions of the same inlet tube cross-

section shown in Figure 3.6: the relevant values obtained for the reference M_2 analysis are shown in Table 3.8, while Table 3.9 reports the percentage differences between the homologous quantities evaluated in the M_1 and M_3 analyses, and those of the reference case M_2 . It can be noted that Table 3.9 exhibits rather low disagreements among the considered cases, confirming that the mesh element size adopted in the reference M_2 analysis endows a mesh-independent evaluation of the fluid velocity profile.

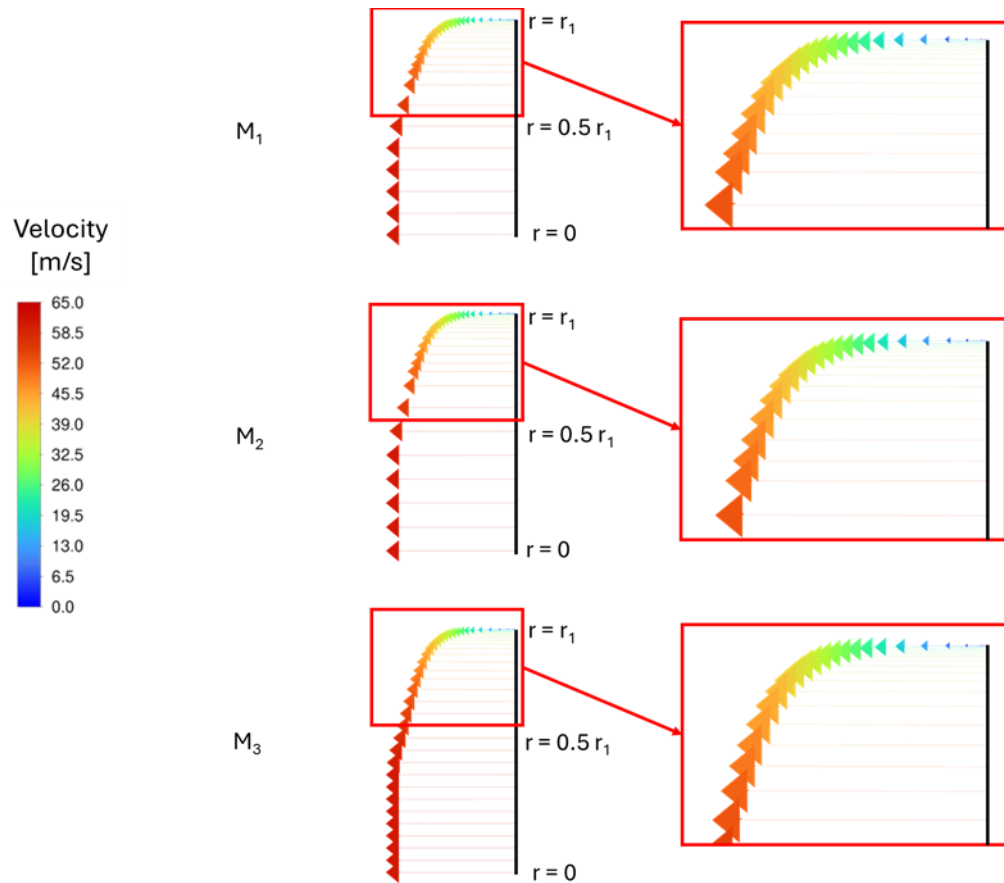


Figure 3.6: Sensitivity of the velocity profile in the inlet tube to mesh element size.

Table 3.8: Fluid velocities at three radial positions in the inlet tube (M_2 analysis).

Radial location	$r = 0$	$r = 0.5 r_1$	$r = 0.98 r_1$
Velocity [m/s]	60.61	59.67	31.90
Normalized velocity	V_∞	$0.98 V_\infty$	$0.53 V_\infty$

Table 3.9: Percentage differences between evaluated fluid velocities at three radial positions in the inlet tube among the analyses M₁, M₂, and M₃.

	r = 0	r = 0.5 r₁	r = 0.98 r₁
M₁	0.18 %	0.15 %	− 2.32 %
M₂	Reference	Reference	Reference
M₃	− 0.11 %	− 0.38 %	0.84 %

Regarding the refinement applied at the fluid-solid interfaces, three analyses have been compared adopting the same mesh element size as the reference M₂ analysis (Table 3.7), and different strategies to discretize near-wall regions. In more detail, the three analyses have been set up so to ensure a substantially identical total thickness of the refined mesh layers, while adopting either a coarser discretization (M₄) or a finer one (M₅) compared with the reference M₂ analysis. The main mesh features of the compared analyses are illustrated in Table 3.10.

Table 3.10: Main mesh features adopted for M₄, M₂ and M₅ analyses.

Analysis	First Layer Thickness [μm]	Layers Growth Rate	Inflation Layer Number	Total Layers Thickness [mm]	Nodes/Elements
M ₄	2	1.51	14	1.25	5.7·10 ⁵ /5.7·10 ⁵
M ₂	2	1.3	20	1.26	6.6·10 ⁵ /6.6·10 ⁵
M ₅	2	1.169	30	1.27	8.7·10 ⁵ /8.7·10 ⁵

The percentage differences of the evaluated total pressure drops are shown in Table 3.11 and indicate that the refinement performed in near-wall regions in the reference M₂ analysis endows a mesh-independent estimate of the total pressure drop.

Table 3.11: Total pressure drop sensitivity to mesh refinement at fluid-solid interfaces.

Analysis	Percentage variation of evaluated total pressure drop
M ₄	− 0.01%
M ₂	Reference
M ₅	+ 0.47%

In line with the previous assessment, also for analyses M₂, M₄ and M₅, a comparison has been performed between the fluid velocity profiles in a section of the inlet tube where the flow is fully developed. Figure 3.7 shows the visual (qualitative) comparison of the velocity

profiles, while the percentage differences of the velocities sampled at three points of the inlet tube are reported in Table 3.12.

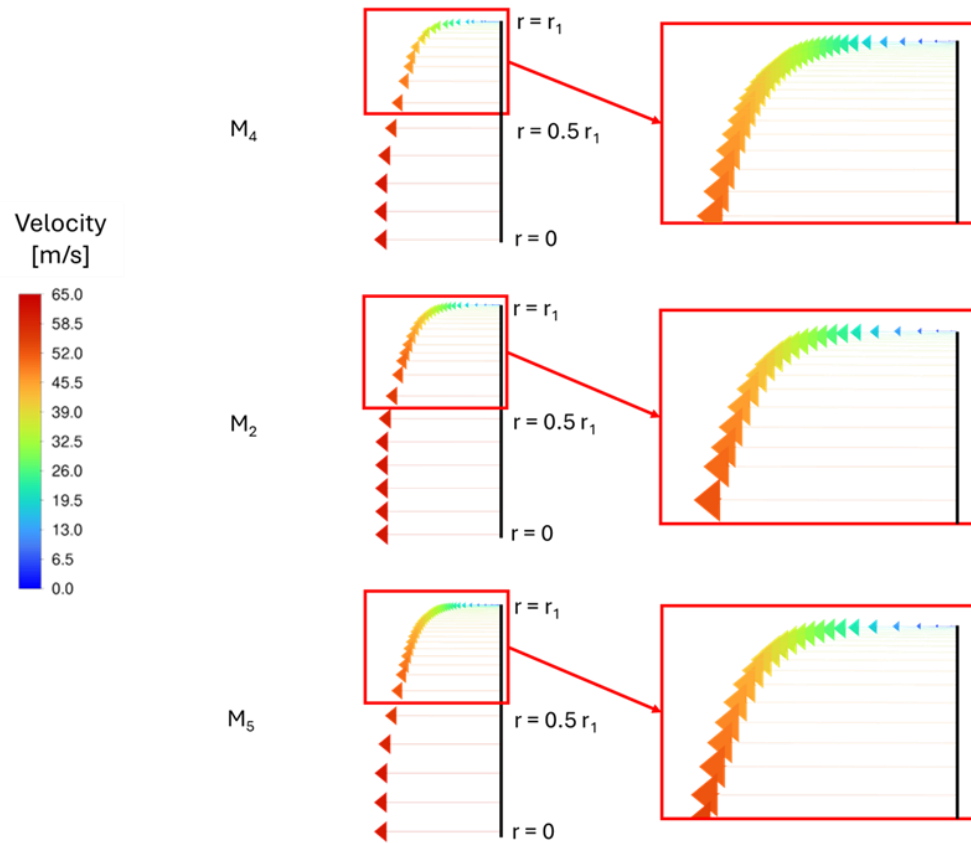


Figure 3.7: Sensitivity of the velocity profile in the inlet tube to mesh refinement at fluid-solid interfaces.

Table 3.12: Percentage differences between evaluated fluid velocities at three radial positions in the inlet tube among the analyses M_4 , M_2 , and M_5 .

	$r = 0$	$r = 0.5 r_1$	$r = 0.98 r_1$
M_4	− 0.35 %	− 0.36 %	− 0.67 %
M_2	Reference	Reference	Reference
M_5	0.15 %	0.09 %	0.18 %

It can be noticed that, considering both qualitative and quantitative indicators, the three different discretizations predict nearly identical velocity profiles, indicating that the mesh refinement in the proximity of fluid-solid interfaces performed in the reference M_2 analysis also allows to reliably capture this kind of local behaviour.

It can be concluded that the models adopted in the thermofluid-dynamic analysis campaign (section 3.5) are adequate to ensure grid-independent solutions.

3.5 Thermofluid-dynamic analysis campaign

In the former sections, the methodology, setup, and grid independence evaluation of the thermofluid-dynamic analysis campaign have been widely described. The present section outlines the different steps and key ideas of the parametric analysis campaign and pin layout optimization. Although the optimization procedure is hereby described in detail, given the broad range of considered parameters and loading conditions, a summary of the outcomes of the thermofluid-dynamic analysis campaign is provided in section 3.6. For the sake of readability, Figure 3.8 provides a graphical representation of the parameters discussed in the present subsection and Table 3.13 summarizes the characteristics of the analysed pin layouts.

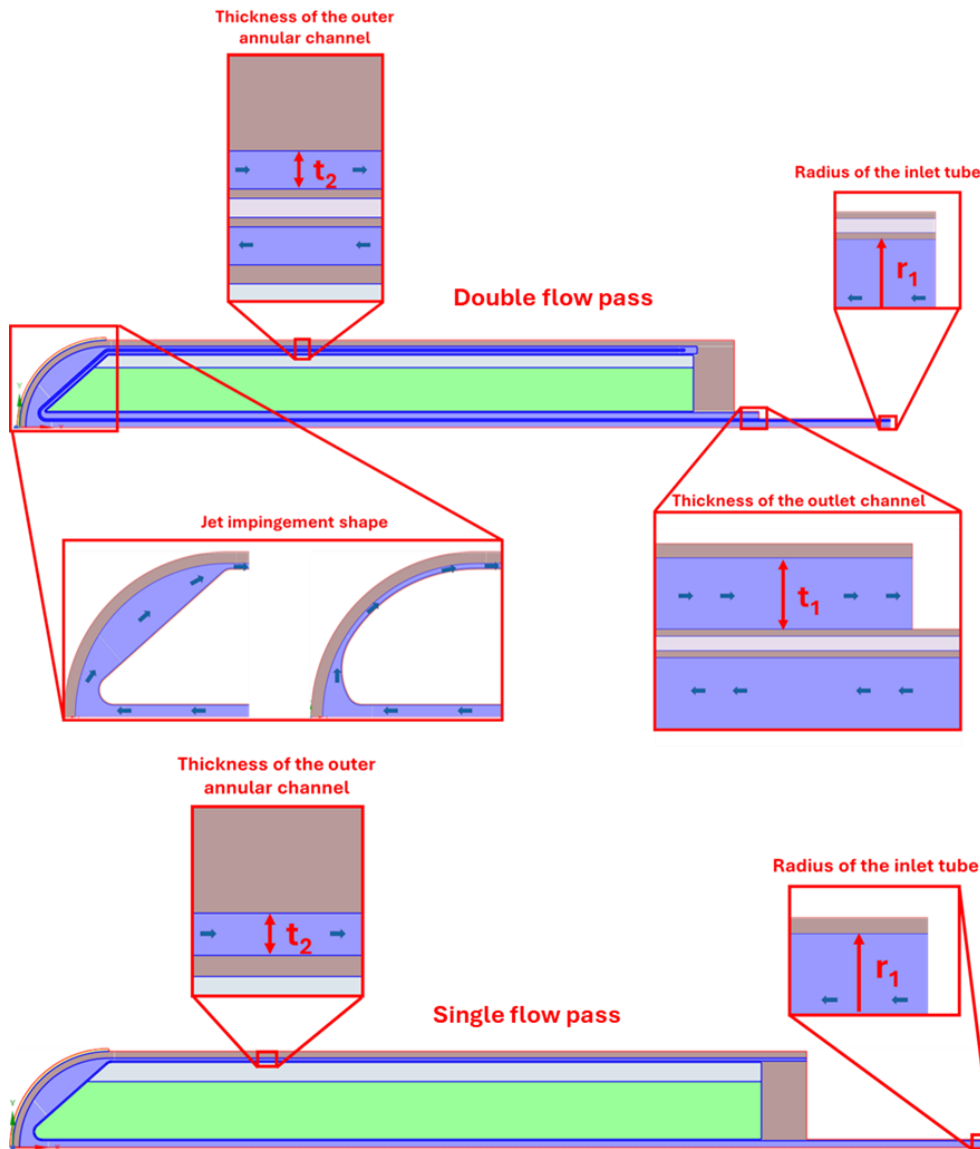


Figure 3.8: Overview of the analysed parameters for double (top) and single (bottom) flow pass pin geometries.

Table 3.13: Characteristics of the pin layouts assessed in the thermofluid-dynamic analysis campaign (differences from the baseline layout highlighted in bold).

Case	Fluid flow pass	Outlet channel thickness t_1 [mm]	Jet impingement shape	Inlet tube radius r_1 [mm]	Outer annular channel thickness t_2 [mm]
1 (Baseline)	Double	5	Baseline	5	2
2	Double	2	Baseline	5	2
3	Double	2* * larger multiplier	Baseline	5	2
4	Double	2	Alternative	5	2
5	Single	Not defined	Baseline	5	2
6	Double	3	Baseline	5	2
7	Double	4	Baseline	5	2
8	Double	1	Baseline	5	2
9	Double	2	Baseline	6	2
10	Single	Not defined	Baseline	4	2
11	Double	2	Baseline	7	2
12	Double	2	Baseline	8	2
13	Single	Not defined	Baseline	6	2
14	Single	Not defined	Baseline	7	2
15	Single	Not defined	Baseline	6	1

In more detail, within the present subsection, firstly (section 3.5.1) the analysis on the baseline HCPB monoblock pin layout is introduced, providing the reader with preliminary information on the pin thermofluid-dynamic behaviour. Following that, the aim is to explore the pin behaviour by changing the outlet channel thickness t_1 , and section 3.5.2 details the analyses aimed at determining whether this reduction shall be performed by enlarging the BZ or by enlarging the stagnant helium channel. Subsequently, the parametric analyses that considered different values of outlet channel thickness t_1 are presented in 3.5.3. At this point, the best layout that emerged (optimized t_1) has been subjected to further investigations (section 3.5.4), and the possibility of changing the jet impingement shape has been explored (section 3.5.5). In 3.5.6, a pin layout (namely case 5) envisaging a single helium flow pass is investigated, and section 3.5.7 illustrates the parametric analyses that considered different values of the inlet tube radius r_1 both for double and single flow pass pin configurations.

Finally, section 3.5.8 details the evaluations performed on the outer annular channel thickness t_2 and section 3.5.9 presents some final remarks on the collected results.

3.5.1 Baseline HCPB monoblock pin analysis campaign

The first analyses conducted on the HCPB monoblock pin involved the baseline (case 1) layout, the geometry of which has been shown in Figure 1.15. The analysis campaign allowed to explore the behaviour of the pin under different heat loading conditions in terms of coolant temperature rise (Table 2.10) and plasma radiation load (Table 2.11). Moreover, the roughness values of Table 2.8 have been considered on surfaces “a” and “b” (see Figure 2.12). As to the discretization, the settings introduced in Table 3.1 have been employed, resulting in the mesh shown in Figure 3.3, constituted by $6.6 \cdot 10^5$ elements and $6.6 \cdot 10^5$ nodes.

The collected results obtained in this analysis campaign are extensively reported in Appendix A (Table A 1 to Table A 5), in the form of tables detailing the examined working conditions, the coolant pressure drop and the maximum temperatures occurring in solid domains. However, a comprehensive overview is hereby provided.

Among the working ranges described in section 2.2 for the HCPB monoblock solid and fluid domains, every condition is met except for the upper temperature limit of 550 °C for parts in Eurofer with a structural function (Table 2.1). In fact, all the investigated cases present maximum temperatures in Eurofer domains exceeding 550 °C. Figure 3.9 shows the temperature distribution relevant to the simulation performed with roughness on surfaces “a” and “b” of 20 μm , HF_0 of 0.3 MW/m^2 and coolant temperature rise of 220 °C, and Figure 3.10 shows the relevant fluid flow field.

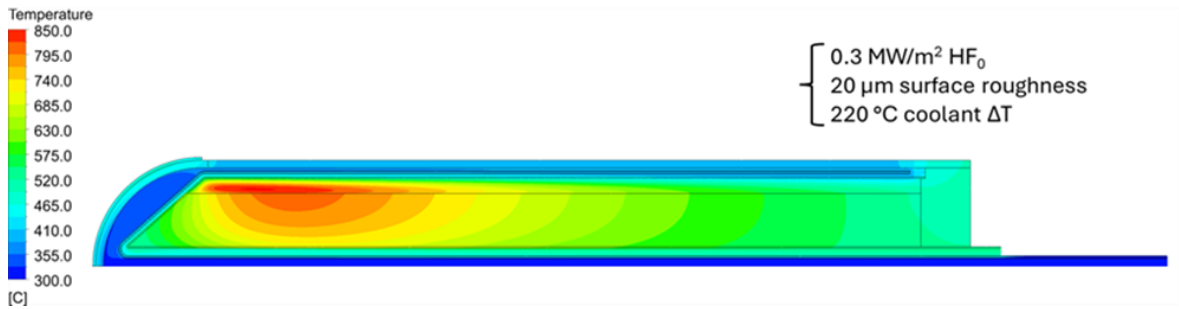


Figure 3.9: Temperature field in solid and fluid domains of case 1 layout.

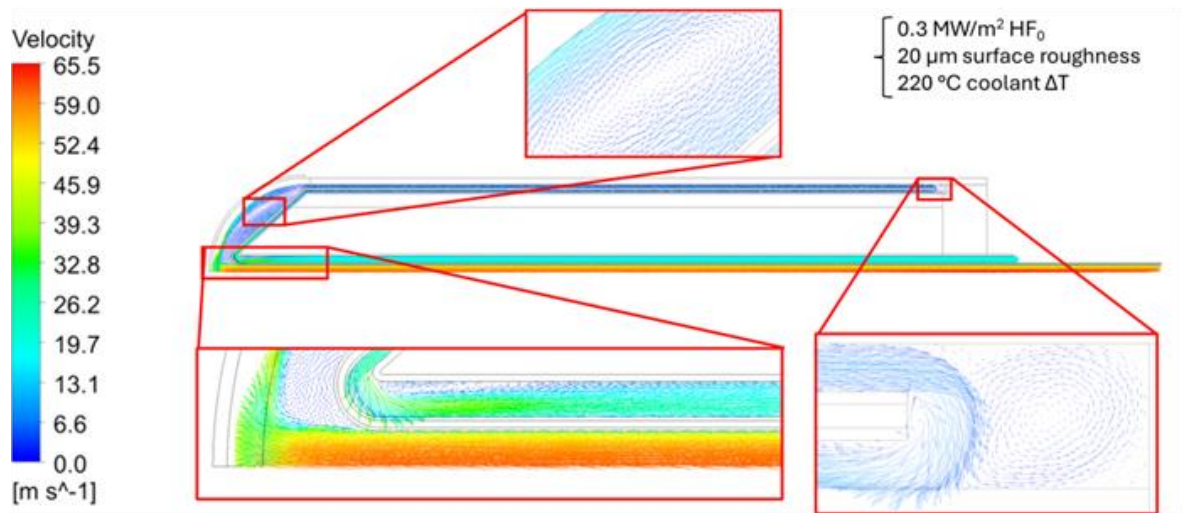


Figure 3.10: Fluid velocity vector plot for case 1 layout: details of vortices and fluid vein detachments.

To provide a single representative indicator of the temperature behaviour in each solid domain of the HCPB monoblock pin, the following procedure has been adopted. For each of the analyses performed with varying roughness on surfaces “a” and “b” (Figure 2.12), ΔT and HF_0 (data in Table A 2, Table A 3, and Table A 4), the maximum temperature occurring within each solid domain was identified. Consequently, each domain was associated with a set of maximum temperature values, one corresponding to each analysis. To summarise these results into a single representative metric for every solid domain, the maximum temperatures obtained for each domain across the considered analyses have been averaged. The relevant values are shown in Figure 3.11.

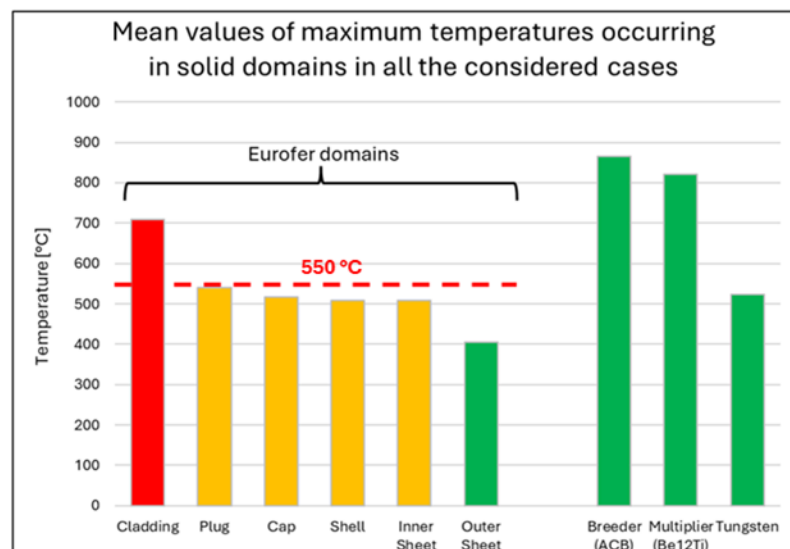


Figure 3.11: Mean values of temperature peaks in the different structures.

Observing Figure 3.11 it emerges that the overall temperature distribution in the tungsten, breeder, and multiplier domains poses no concerns (thanks to wide margins against their

respective design limits of 1050 °C, 970 °C, and 1050 °C, introduced in section 2.3), while multiple Eurofer domains are close to the 550 °C threshold, and the cladding is far beyond. In more detail, the Eurofer cladding is not considered to have a structural function in the pin as it only contains the functional material without sustaining a pressure difference, therefore in principle this part of the domain is not required to comply with the Eurofer upper temperature limit of 550 °C. Moreover, it is expected that the cladding will be object of further structural studies in which the use of other materials (e.g. SiC) may be proposed, also in view of a better compatibility with the ceramic material and lower tritium permeation rates. For these reasons, the high cladding temperatures alone shall not be deemed showstoppers for the HCPB monoblock in the present stage of the design. Nevertheless, temperatures as high as those occurring in the case 1 layout may jeopardize reliability of the analysed HCPB concept (envisaging a cladding made of Eurofer) due to thermal stresses, therefore it is desirable to find a configuration in which the cladding is cooled more effectively.

Figure 3.12 shows the maximum temperatures occurring in the Eurofer cap and cladding of the baseline pin layout along with the coolant total pressure drop when considering different intensities of the radiative heat flux (HF_0) with fixed coolant temperature rise of 220 °C and fixed roughness of coolant-wetted surfaces of 20 μm .

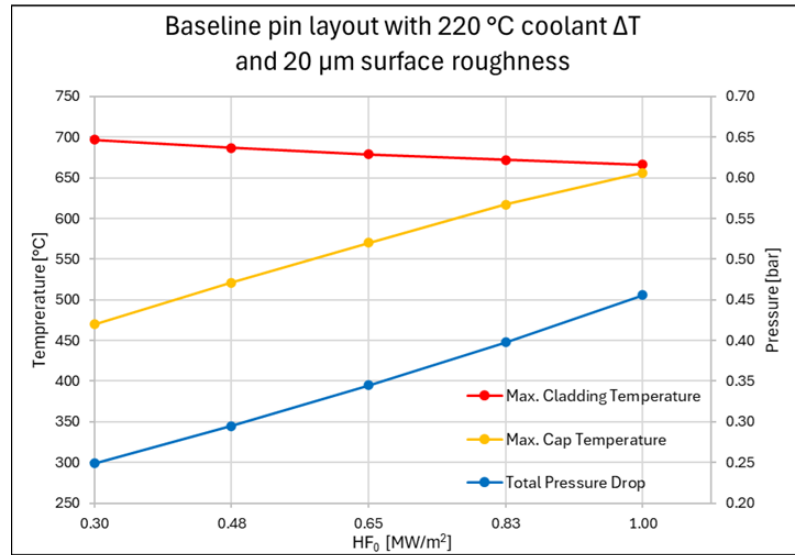


Figure 3.12: Peak temperatures in Eurofer cap and cladding, and coolant total pressure drop observed in case 1 layout when increasing HF_0 with fixed ΔT and surface roughness.

It can be observed that increasing HF_0 produces a reduction in the maximum Eurofer temperature as it improves the thermal field in the hottest region of the Eurofer domain – located in the cladding – due to the increased MFR (required for a fixed coolant temperature rise, according to Eq. 3.28). However, the cap maximum temperature increases, exceeding

550 °C for HF_0 values higher than 0.6 MW/m². As the relevant hotspot is rather localized, further structural analyses may reveal that overcoming the temperature design limit does not represent an issue under these conditions. In fact, further thermomechanical studies may reveal cooperation mechanisms between the material located in the hotspot and its surroundings, subjected to less severe thermal loading. Nevertheless, according to the present knowledge, temperatures exceeding 550 °C in the Eurofer cap should pose a concern.

Additionally, the following trends emerged from the analysis campaign on the case 1 pin layout:

- an increase of sand grain roughness on machined surfaces allows for a reduction of the maximum Eurofer temperature (up to 11 °C when it changes from 20 µm to 100 µm) as it enhances the heat exchange in the cladding;
- although appealing for the overall plant efficiency (see 2.4.1), increasing the coolant temperature rise beyond 220 °C appears to be particularly prohibitive. In fact, for each 1 °C increase in the coolant temperature rise, the maximum Eurofer temperature is increased by 1.4 °C. Following this observation, the combination of heat loads characterised by HF_0 of 0.3 MW/m² and ΔT of 220 °C has been taken as reference working condition for the HCPB monoblock pin;
- the total pressure drop exhibits a slight sensitivity to roughness and a pronounced dependence on the MFR, which can be deduced from the trend observed in Figure 3.12. Moreover, the pressure gauge observed in the case 1 configuration is rather low, always showing wide margins against the estimated limit of 0.8 bar introduced in section 2.2.2.

The results collected for the baseline HCPB monoblock pin layout and reported above allowed to draw the following conclusions:

- in order to improve the thermal behaviour of the concept it is necessary to enhance the fluid-solid heat exchange in the Eurofer cladding region;
- potentially, the HCPB monoblock pin may operate effectively with HF_0 values higher than the reference 0.3 MW/m²;
- surface finishing in critical surfaces helps in lowering the maximum Eurofer temperature;
- only a substantial design improvement may allow HCPB monoblock pin operation with coolant temperature rise higher than 220 °C;

- the total pressure drop occurring in the pin is rather low, hence it is possible to explore solutions which improve the thermal field at the cost of a worsened fluid-dynamic behaviour.

Based on these findings, it seems appealing to explore the pin behaviour with lower values of the outlet channel thickness t_1 (see Figure 2.3). In fact, reducing the section of the outlet annular channel would result in higher fluid flow velocities in the same region, with two implications: on the one hand, the fluid-solid heat exchange in this region will be enhanced, which is of particular interest as – in the outlet channel – the fluid is interacting with the hottest regions of the Eurofer cladding. On the other hand, the fluid will observe a higher pressure drop in the outlet channel, which is an undesirable effect that can be tolerated as long as the total pressure drop occurring within the pin remains lower than the estimated design limit.

Once identified the outlet thickness t_1 as a key performance parameter to be investigated, the following step of the analysis has been focused on determining the most convenient way to reduce t_1 . In fact, as described in section 2.3.1, a reduction of the outlet channel thickness in the baseline pin layout can be pursued both by enlarging the stagnant helium thickness and by enlarging the BZ.

3.5.2 Comparison between case 2 and case 3 layouts

Preparatory to a parametric analysis on the outlet channel thickness t_1 , the study presented in this section is aimed at determining whether the reduction of the outlet section should be performed by enlarging the stagnant helium thickness or by enlarging the BZ. With this scope, considering that the baseline (case 1) layout is characterised by t_1 of 5 mm, two layouts have been analysed with a rather lower value of t_1 – namely 2 mm – ensuring appreciable differences in the thermofluid-dynamic behaviour of the pin compared to the baseline.

In more detail, the two new layouts, characterised by a t_1 of 2 mm and named the case 2 and case 3 layouts, differ because in the case 2 geometry, the reduction of t_1 (compared to the baseline) is achieved by enlarging the stagnant helium channel, while in the case 3 geometry, it is performed by enlarging the BZ. The resulting geometries have been showed in Figure 2.4, which is hereby reported for further clarity (Figure 3.13) along with Table 3.14 resuming the respective geometric features.

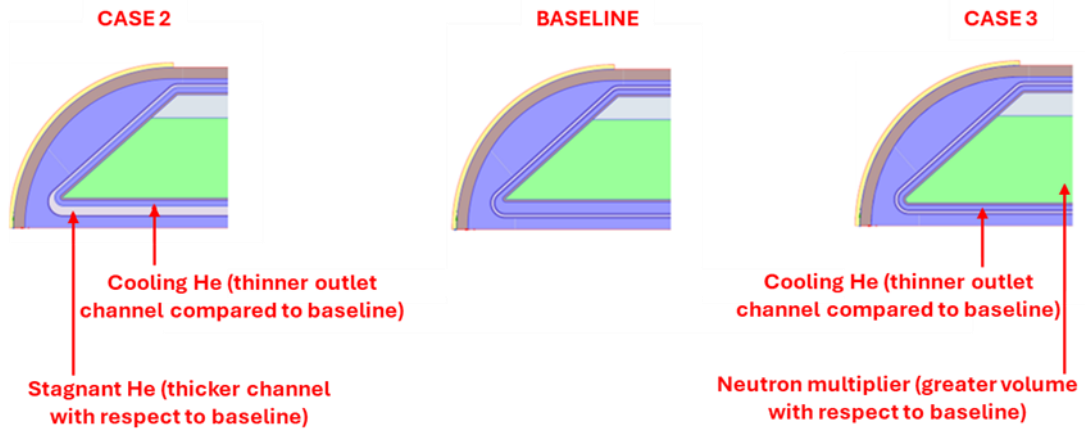


Figure 3.13: Apical area of baseline (case 1) geometry (middle), case 2 and case 3 (sides).

Table 3.14: Summary of geometric characteristics of case 1, case 2, and case 3 layouts.

Layout	case 1	case 2	Case 3
t_1 [mm]	5	2	2
Geometric feature	- (baseline)	stagnant helium channel larger than baseline	breeding zone larger than baseline

With the scope to obtain a reliable comparison between the two layouts, also in the analysis of case 2 and case 3 geometries several combinations of loads and parameters have been investigated. In more detail, all the values of coolant temperature rise (Table 2.10) and plasma radiation load (Table 2.11) have been considered. Moreover, the roughness values of Table 2.8 were applied on surfaces “a” and “b” (see Figure 2.12). As to the discretization, the settings introduced in Table 3.1 have been employed, resulting in the mesh characteristics outlined in Table 3.15.

Table 3.15: Summary of discretization features of case 2 and case 3 geometries.

Layout	N. of mesh nodes	N. of mesh elements
case 2	$7.1 \cdot 10^5$	$7.1 \cdot 10^5$
case 3	$6.8 \cdot 10^5$	$6.8 \cdot 10^5$

The collected results obtained in this analysis campaign are extensively reported in Appendix A (Table A 6 to Table A 10 for case 2 and Table A 11 to Table A 15 for case 3). However, the outcomes of the comparison between the two design alternatives are hereby outlined.

In order to provide an overall and comprehensive comparison between the two layouts, the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on surfaces “a” and “b”, ΔT and HF_0

(data in Table A 7, Table A 8, and Table A 9 for case 2 and Table A 12, Table A 13, and Table A 14 for case 3) have been averaged and the relevant values are compared in Figure 3.14.



Figure 3.14: Comparison between case 2 and case 3 pin layout performance.

Observing Figure 3.14, the case 2 layout clearly emerges as a better design option than case 3 when considering the thermal and fluid dynamic performance. In fact, case 2 geometry presents a 9°C lower maximum Eurofer temperature with a total pressure drop that is 30% lower than that of the case 3. This can be explained considering that the increased multiplier volume of case 3 geometry results in higher deposited power, and that the cross-section of the outlet annular channel of case 3 layout is smaller compared to that of case 2 geometry, as it is located closer to the X symmetry axis.

This clear difference in the results has allowed to determine that, with the purpose to investigate the pin's thermal and fluid dynamic behaviour with different (parametrised) values of the outlet channel thickness t_1 smaller than the baseline, the reduction of the outlet section is to be performed by enlarging the stagnant helium channel (as done in the case 2 configuration) rather than by enlarging the BZ (as in the case 3 layout). This conclusion paved the way for the parametric analysis on the outlet channel thickness t_1 , detailed in the following section.

3.5.3 Parametric analysis on outlet channel thickness t_1

Although previously mentioned (section 2.3.1), the examined t_1 values and the respective layout names are hereby reported for the sake of readability (Table 3.16). Moreover, Figure 3.15 shows the case 1 ($t_1 = 5$ mm) and case 8 ($t_1 = 1$ mm) geometries for further clarity.

Table 3.16: Examined t_1 values.

t_1 [mm]	5	4	3	2	1
Name of the layout	case 1 (baseline)	case 7	case 6	case 2	case 8

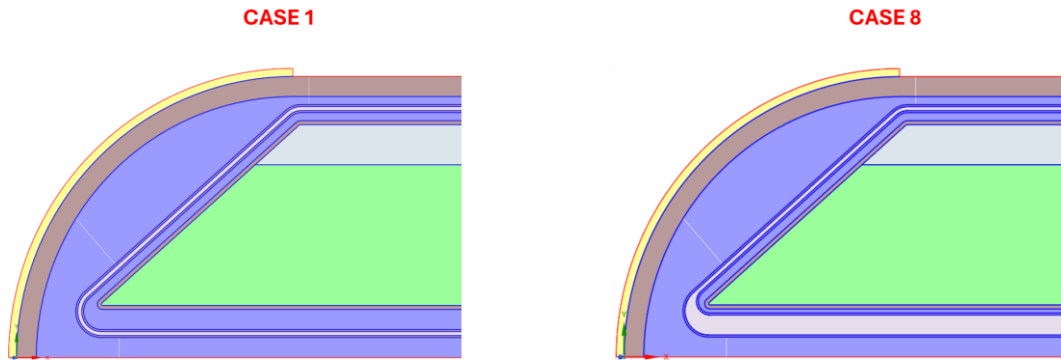


Figure 3.15: Apical area of case 1 (baseline) geometry (left), and case 8 (right).

With the scope to obtain a reliable comparison between the investigated layouts, and to exploit the low computational burden characterising the analyses for gathering comprehensive data, also in the analysis of these geometries several combinations of loads and parameters have been investigated. In more detail, all the values of coolant temperature rise (Table 2.10) and plasma radiation load (Table 2.11) have been considered. Moreover, the roughness values of Table 2.8 were applied on surfaces “a” and “b” (see Figure 2.12). As to the discretization, the settings introduced in Table 3.1 have been employed, resulting in the mesh characteristics outlined in Table 3.17. Please note that the data relevant to the case 1 and the case 2 layout have already been presented in sections 3.5.1 and 3.5.2 respectively, and are hereby reported for completeness.

Table 3.17: Summary of discretization features for parametric analysis on t_1 .

Layout	N. of mesh nodes	N. of mesh elements
case 1	$6.6 \cdot 10^5$	$6.6 \cdot 10^5$
case 7	$7.0 \cdot 10^5$	$7.0 \cdot 10^5$
case 6	$7.0 \cdot 10^5$	$7.0 \cdot 10^5$
case 2	$7.1 \cdot 10^5$	$7.1 \cdot 10^5$
case 8	$7.0 \cdot 10^5$	$7.1 \cdot 10^5$

The collected results obtained in the parametric analysis are extensively reported in Appendix A (Table A 16 to Table A 20 for case 7, Table A 21 to Table A 25 for case 6, and Table A 26 to Table A 30 for case 8). However, the outcomes of the comparison between the design alternatives are hereby illustrated.

In order to provide an overall and comprehensive comparison between the examined layouts, the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on surfaces “a” and “b”, ΔT and HF_0 (data in Table A 2 to Table A 4 for case 1, Table A 17 to Table A 19 for case 7, Table A 22 to Table A 24 for case 6, Table A 7 to Table A 9 for case 2, and Table A 27 to Table A 29 for case 8) have been averaged and the relevant values are compared in Figure 3.16.

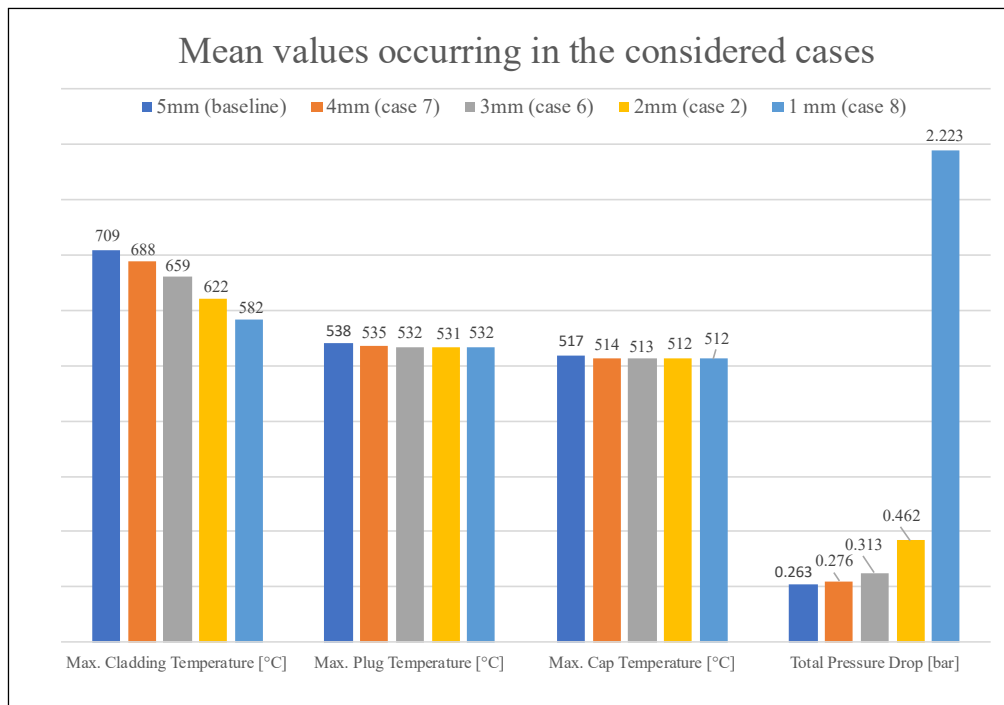


Figure 3.16: Comparison of the performance of the pin layouts with different t_1 .

Observing Figure 3.16 it can be noted that, as expected, reducing the outlet channel thickness results in lower temperatures occurring in the Eurofer cladding, at the expense of a higher coolant pressure drop. Moreover, it is particularly noticeable the abrupt increase in the total pressure drop that occurs when passing from the case 2 layout – envisaging a 2 mm thick outlet channel – to the case 8 layout, characterized by a 1 mm thick outlet channel. It has been retained worth to verify that this trend arises from the physics of the problem rather being originated by some numerical error. To this end, approximate evaluations have been performed on the rate of growth of the pressure loss occurring in the outlet channel due to

the reduction of the thickness, estimating it analytically and comparing the result to the homologous trend inferable from Figure 3.16.

Analytically, the pressure drop ΔP occurring in the channel can be estimated by the Darcy-Weisbach equation:

$$\Delta P = \frac{1}{2} f \left(\frac{L}{D_h} \right) \rho v^2 \quad (3.29)$$

Where f is the Darcy friction factor, L the length of the considered flow path, D_h the hydraulic diameter of the flow section, ρ the fluid density, and v the fluid velocity.

The purpose being to evaluate the ratio between the pressure losses occurring in the outlet channel of different pin layouts, the effect of fluid density can be retained analogous in the considered cases and thus vanishes in the comparison. Moreover, the length of the channel L is the same in all the configurations. To perform an approximate evaluation, the variations of the Darcy friction factor f can be neglected, and fluid velocity v can be assumed to be inversely proportional to the flow cross section A .

Under these assumptions, the ratio between the annular channel pressure drops of two layouts characterized by thicknesses t_a and t_b will be:

$$\frac{\Delta P_b}{\Delta P_a} = \frac{D_{h_a} A_a^2}{D_{h_b} A_b^2} \quad (3.30)$$

Choosing the baseline layout (with an annular channel thickness of 5 mm) as the reference t_a value used to evaluate Eq. 3.30, the estimated normalized pressure losses occurring in the outlet channels of the layouts compared in Figure 3.16 are shown in Table 3.18.

Table 3.18: Estimated normalized pressure loss in annular channel for different thickness values.

t_1 [mm]	5	4	3	2	1
Estimated normalized pressure loss	1	1.76	3.79	11.65	85.30

It can be observed that, under the mentioned hypotheses, the normalized pressure drop rises dramatically when passing from t_1 of 2 mm to t_1 of 1 mm.

To infer an analogous trend on the outlet channel pressure loss from Figure 3.16, it is necessary to isolate its contribution on the pressure drop from the total pressure loss occurring during the flow path. Considering the results that will be detailed in the following and presented in Table 3.20, it should be taken into account that the pressure losses in Figure 3.16 are averaged among simulations employing different roughness values on surfaces “a” and “b”, most of which envisaging 20 μm . Hence, the outlet annular channel contribution to the averaged pressure drop of the case 2 layout may be assumed to be around 48 - 49 %, the

remaining 52 - 51 % occurring in the remaining of the geometry, which is identical in the compared layouts. To perform an approximate evaluation, it has been assumed that a pressure loss of 0.24 bar – picked from the range 51 - 52 % of the case 2 pressure loss in Figure 3.16 – occurs in the remaining of the pin geometries, and hence, for each pin layout the average pressure drops occurring in the outlet channels have been deduced from the respective values shown in Figure 3.16 by subtracting 0.24 bar. The inferred normalized pressure drops obtained through this evaluation are reported in Table 3.19 along with the values estimated analytically and presented above.

Table 3.19: Estimated and inferred normalized pressure losses in annular channel for different thickness values.

t_1 [mm]	5	4	3	2	1
Estimated normalized pressure loss	1	1.76	3.79	11.65	85.30
Inferred normalized pressure loss	1	1.57	3.17	9.65	86.22

Considering the multiple approximations introduced in the evaluation, Table 3.19 shows a satisfactory agreement between the analytical and the inferred trend of the outlet channel pressure loss, hence the trend observed in Figure 3.16 appears to arise from the actual physics of the problem rather than from numerical errors.

Once verified that the data collected and shown in Figure 3.16 are a reliable source to consider, it is possible to make some considerations on which t_1 values should be retained more convenient for the HCPB monoblock pin. To this end, the case 8 layout ($t_1 = 1$ mm) is excluded because in every examined working condition it exhibits a coolant pressure drop that is beyond the limit of 0.8 bar. Moreover, unlike all the other variants discussed above, the case 8 also presents some violations of the upper limit for coolant velocity of 100 m/s (section 2.2.2). In more detail, coolant velocities exceed 100 m/s in the 1 mm thick outlet channel – namely the second smallest fluid flow section present in any considered layout, the first being the inlet tube with a radius of 4 mm featured by case 10 – in certain conditions, specifically in the analyses characterized by HF_0 values beyond 0.475 MW/m^2 . Among the other design alternatives, the case 2 layout ($t_1 = 2$ mm) appears to be particularly appealing as it exhibits the lowest maximum Eurofer temperatures with a pressure drop that – despite being the highest – benefits from wide margins against the prescribed design limits. Hence, the t_1 value of 2 mm is selected as the dimension providing the best trade-off between lower Eurofer temperatures and higher coolant pressure drops.

Once selected as the best layout among the examined variants, the case 2 layout has been subject in-depth analysis aimed at exploring the possibility of extending the areas interested by surface finishing and increasing the roughness value beyond 100 μm . Moreover, the pressure drop distribution along the flow path was evaluated to gather information for further improvements.

3.5.4 In-depth analysis on the case 2 layout

As an outcome of the analyses detailed in sections 3.5.1, 3.5.2 and 3.5.3, the case 2 layout ($t_1 = 2 \text{ mm}$) emerged as the one presenting the most appealing thermal and fluid-dynamic behaviour. Moreover, it emerged that – as expected – increasing the value of the roughness on machinable surfaces helps in lowering the maximum Eurofer temperatures. Consequently, it has been retained of interest to investigate more in-depth the behaviour of this pin configuration, by adding “surface c” (see Figure 2.12) to the machined surfaces, by exploring higher roughness values (Table 2.9), and by evaluating the pressure drop breakdown along the coolant flow path. To obtain reliable outcomes in such conditions, the roughness model implemented on Ansys Fluent has been updated from the Standard to the High Roughness (Icing) model, and, accordingly, the adopted mesh features have been updated from those of Table 3.1 to those of Table 3.2, which resulted in a mesh composed of $8.8 \cdot 10^5$ elements connected in $8.8 \cdot 10^5$ nodes. Once again, the analyses considered the HF_0 and ΔT values of Table 2.11 and Table 2.10 respectively.

The collected results obtained in the parametric analysis are extensively reported in Appendix B (Table B 1 to Table B 5). However, the outcomes of the analysis are outlined hereby.

The conducted in-dept analysis campaign has revealed, once again, maximum Eurofer temperatures higher than 550 $^{\circ}\text{C}$ in every parameter and load combination that has been considered. Nevertheless, in some cases, the Eurofer thermal field is very close to the prescribed limit, as one can observe from the trends shown in Figure 3.17.

In detail, comparing Figure 3.17 and Figure 3.12, it can be deduced that the case 2 layout with 200 μm roughness on surfaces “a”, “b”, and “c” benefits from a maximum Eurofer temperature reduction of over 130 $^{\circ}\text{C}$ with respect to the baseline pin geometry under reference working conditions.

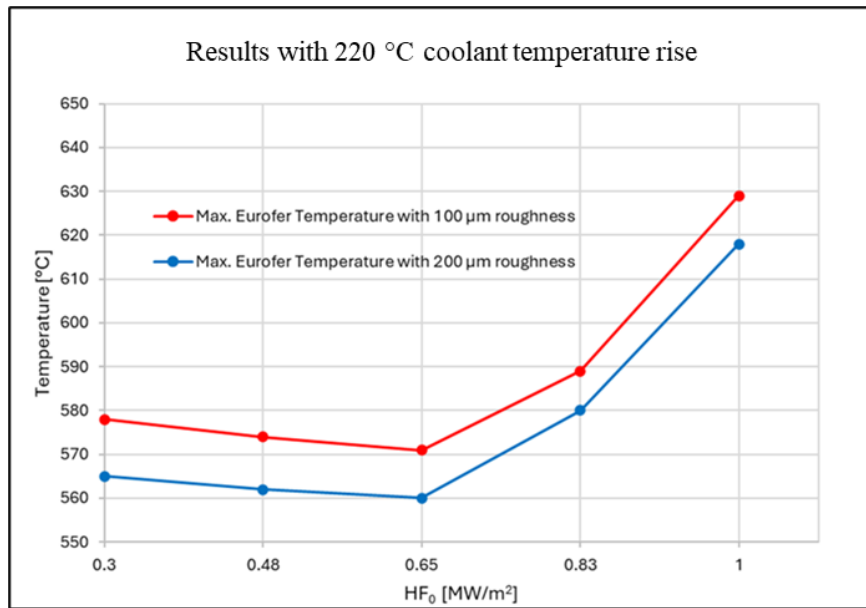


Figure 3.17: Maximum Eurofer temperatures as a function of plasma radiation load in high roughness analyses on case 2 layout.

Moreover, it is worth noting that the non-monotonic trend in maximum Eurofer temperature for case 2 with increasing values of HF_0 (Figure 3.17) is due to the fact that, at some point between 0.65 MW/m^2 and 0.83 MW/m^2 HF_0 , the location of the highest Eurofer temperature shifts from the cladding to the cap. Hence, this trend arises from the strongly improved thermal behaviour that is achieved in the cladding with the updated t_1 value of 2 mm. On the other hand, the use of a lower t_1 and of high roughness values result in higher coolant pressure drop, as can be seen in Figure 3.18, where values higher than 0.8 bar occur when increasing HF_0 beyond 0.65 MW/m^2 with 100 µm roughness on surfaces “a”, “b”, and “c” and over 0.48 MW/m^2 with 200 µm.

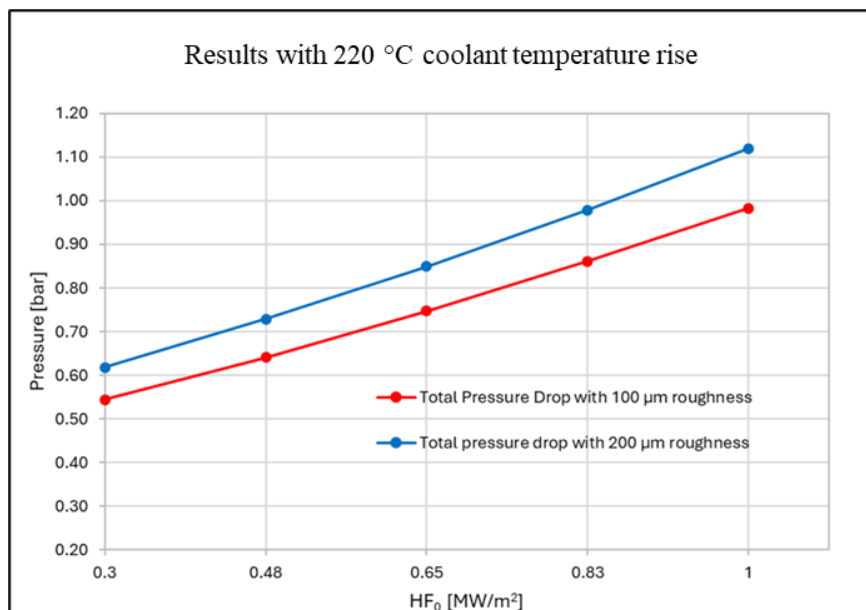


Figure 3.18: Total pressure drop as a function of plasma radiation load in high roughness analyses on case 2 layout.

Further interesting findings have emerged from the evaluation of the pressure drop breakdown along the flow path. In fact, it has been found that, among the considered parameters, the pressure drop breakdown along the flow path (Table 3.20) is only sensitive to the roughness value on machined surfaces. Notably, in all the investigated cases, 94% of the pressure drop occurred in solely three regions, namely the inlet tube, the cap, and the outlet channel. Hence, to reduce the pumping power required by the HCPB monoblock BB, attention should be focused on the fluid-dynamic optimisation of these three regions.

Table 3.20: Pressure drop breakdown (fraction of the total pressure drop) in case 2 pin.

Roughness on machined surfaces [μm]	Fraction of total pressure drop [%]			
	<i>Inlet tube</i>	<i>Cap</i>	<i>Outlet Channel</i>	<i>Other regions</i>
20	31	16	47	6
100	26	14	54	6
200	23	11	60	6

In light of the results presented in this section, two possible pin design updates have been ideated and tested, as described in sections 3.5.5 and 3.5.6.

3.5.5 Investigation on an alternative jet impingement shape

The in-depth analysis on the case 2 pin layout presented in section 3.5.4 has shown that the location of the highest Eurofer temperature shifts from the cladding to the cap at high HF_0 values, and also that a relevant contribution to the total pressure drop is constituted by the pressure loss occurring in the cap (Table 3.20). This evidence has drawn the attention to the shape of the jet impingement region of the HCPB monoblock pin. In more detail, a new pin geometry has been developed, namely the case 4 layout (Figure 3.19), by solely modifying the geometry of the cap region with respect to the case 2, thus maintaining the optimal t_1 value of 2 mm.

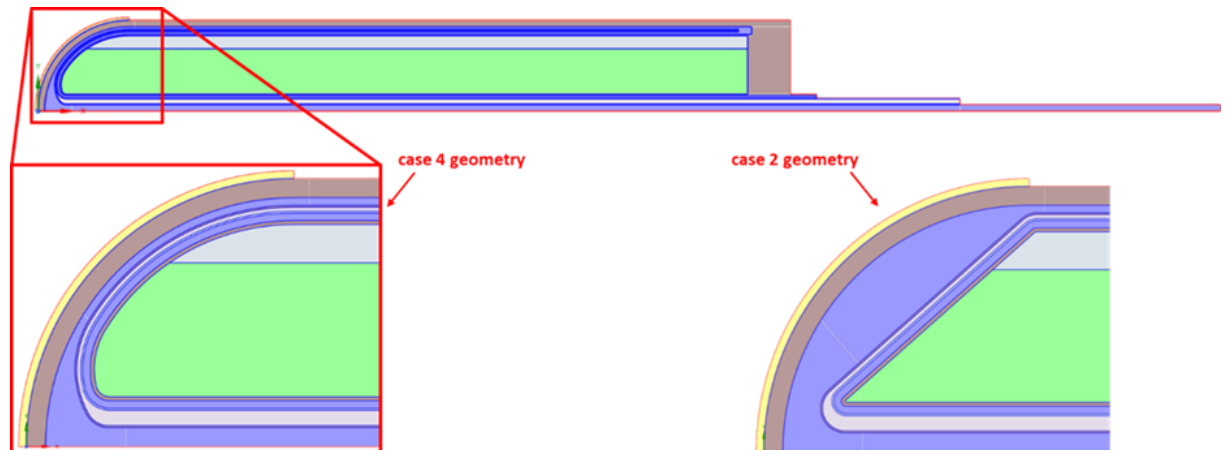


Figure 3.19: Case 4 geometry and comparison with case 2 layout.

The new geometry envisages a 2 mm clearance for the cooling helium flow in the proximity of the Eurofer cap and is aimed at investigating whether a more controlled helium flow in the cap region locally enhances the heat transfer.

The analyses on case 4 layout explored high roughness values (Table 2.9) on surfaces “a”, “b”, and “c”, adopting the Ansys High Roughness (Icing) model, and, accordingly, the mesh features of Table 3.2, which resulted in a mesh composed of $8.8 \cdot 10^5$ elements connected in $8.8 \cdot 10^5$ nodes. Once again, the analyses considered the HF_0 and ΔT values of Table 2.11 and Table 2.10 respectively.

The collected results obtained in the parametric analysis are extensively reported in Appendix B (Table B 6 to Table B 10). However, the outcomes of the analysis are outlined hereby.

In order to evaluate the performance of the new jet impingement shape, it has been decided to perform a comparison between the case 4 and the case 2 layout – which only differ for the shape of the cap region – in relevant pin working scenarios. As a consequence, to provide an overall and comprehensive comparison between the two layouts, the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on surfaces “a”, “b”, and “c”, ΔT and HF_0 (data in Table B 2 to Table B 5 for case 2 and Table B 7 to Table B 10 for case 4) have been averaged and the relevant values are compared in Figure 3.20. Please note that the significant differences between the pressure drops of the case 2 layout indicated in Figure 3.14 and Figure 3.20 are mainly due to the fact that different working conditions have been considered for the two graphs. Hence, this difference does not represent an error, as the data contained in the figures are solely used to compare the relevant pin design variants (case 2 vs case 3 in Figure 3.14 and case 2 vs case 4 in Figure 3.20) under the same working conditions.

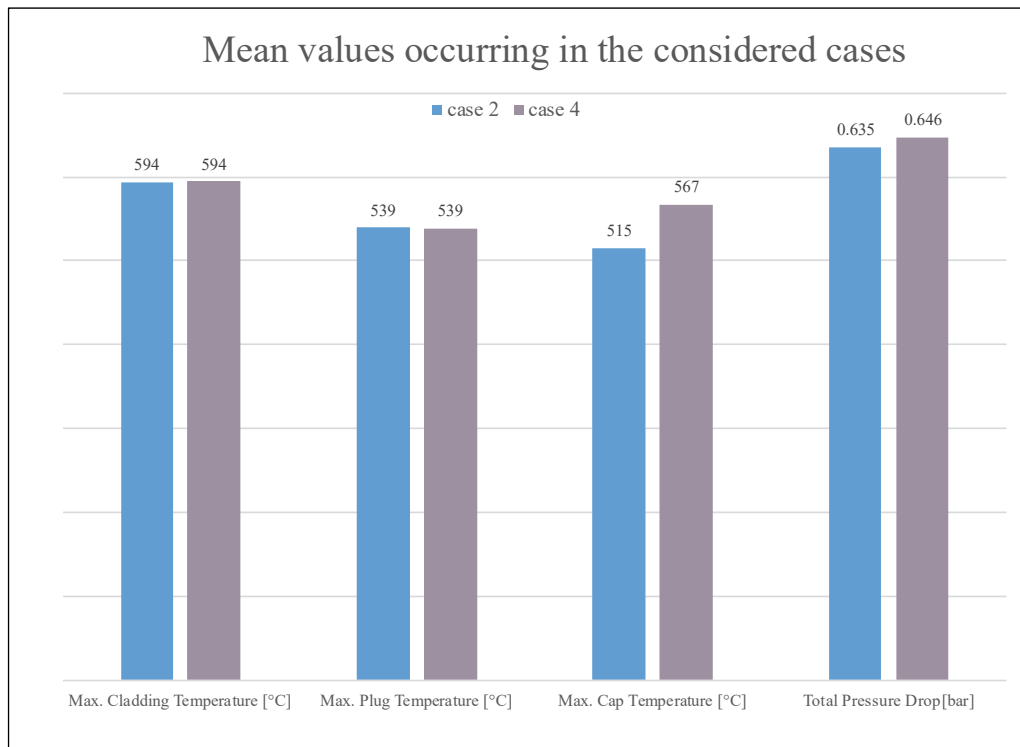


Figure 3.20: Comparison between case 2 and case 4 pin layout performance.

Comparing the performances of the pin, it emerges that the jet impingement shape of the case 4 layout causes a substantial worsening of the cap thermal field. This can be explained by observing Figure 3.21: the jet impingement geometry of case 4 causes lower cooling helium velocities in the proximity of the Eurofer cap compared to case 2, which implies a less intense heat transfer, and, finally, higher temperatures within the cap.

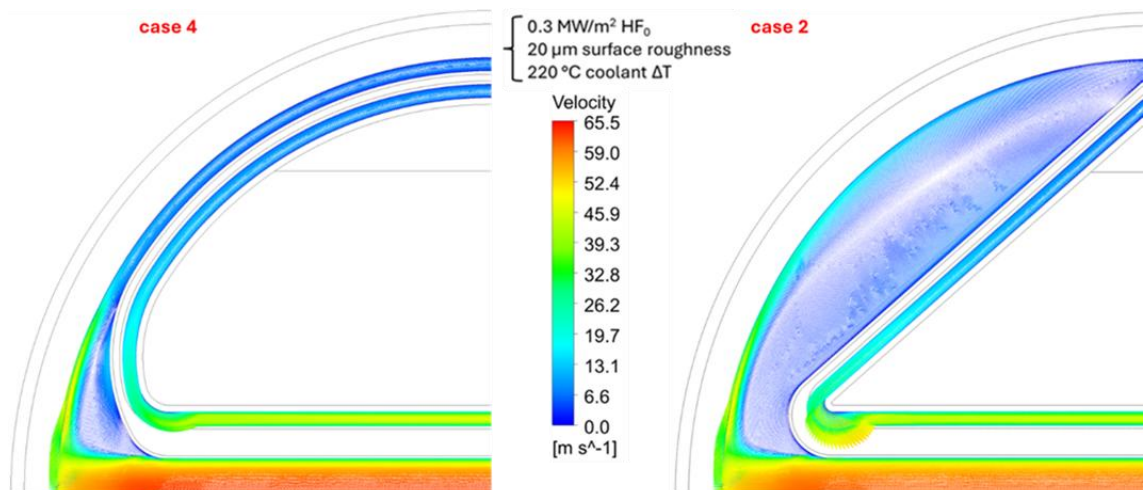


Figure 3.21: Fluid velocity vector plot in jet impingement region of case 4 and case 2 layouts.

As to the pressure drop (Figure 3.20), on average, the case 4 geometry presents a pressure drop that is roughly 2% higher than that of the case 2 layout. It is worth noting that this increase is lower than 5%, which is the increase predicted by applying a quadratic model for pressure drop dependence on velocity, and extrapolating the value that would have been

obtained in case 2 layout with the increased average inlet velocity occurring in case 4 layout (that is due to the increased MFR, consequence of greater breeder and multiplied volume and hence greater deposited power, which requires higher MFR for given coolant temperature rise). This trend is a consequence of the fact that the total pressure drop occurring in the cap region is lower in the case 4 design than in the case 2 layout, whereas the remaining part of the geometry is equal in the two layouts. However, the slight reduction in the pressure loss occurring in the cap is by no means an advantage that can justify a preference for the case 4 layout, due to the major deterioration of the cap thermal performance.

In light of the results outlined above, the jet impingement shape of the case 4 layout has not been adopted in the subsequent steps of the thermofluid-dynamic optimization.

3.5.6 Analysis on a pin layout with a single flow pass

All the analyses presented above are characterised by maximum Eurofer temperatures higher than 550 °C, with many combinations of heat loads and parameters in which solely the Eurofer cladding exceeds this limit. This observation has drawn the attention to coolant flow conditions in the proximity of the cladding. In the examined layouts, which feature a double coolant flow pass through the pin domain, the coolant flows adjacent to the Eurofer cladding – thereby cooling the breeder zone (BZ) – only after completing its first pass through the pin domain and absorbing thermal power from the cap and shell. The rationale behind this design – where the coolant entering the cap through the inlet tube is isolated from the BZ by a stagnant helium layer and by the coolant counterflowing in the outlet channel – is to prioritize cooling of the Eurofer cap, which is strongly affected by heat flux from the tungsten plasma-facing surface as well as by nuclear heating.

However, considering the results discussed in the previous sections – which show significant temperature margins for the cap with respect to the 550 °C limit under low (yet realistic) HF_0 values – it was deemed worth to explore a design that enhances heat transfer in the cladding region, even at the expense of a worsened thermal behaviour in the cap. Consequently, a new pin layout was conceived (namely the case 5 layout, shown in Figure 3.22), where the stagnant helium layer (and thus the metal sheets) is removed, resulting in a geometry with a single coolant flow pass that exchanges thermal power with the BZ and cladding throughout most of its path. In this new configuration, the “cold” coolant entering the pin domain absorbs heat from the BZ as it flows through the inlet tube, before reaching the cap.

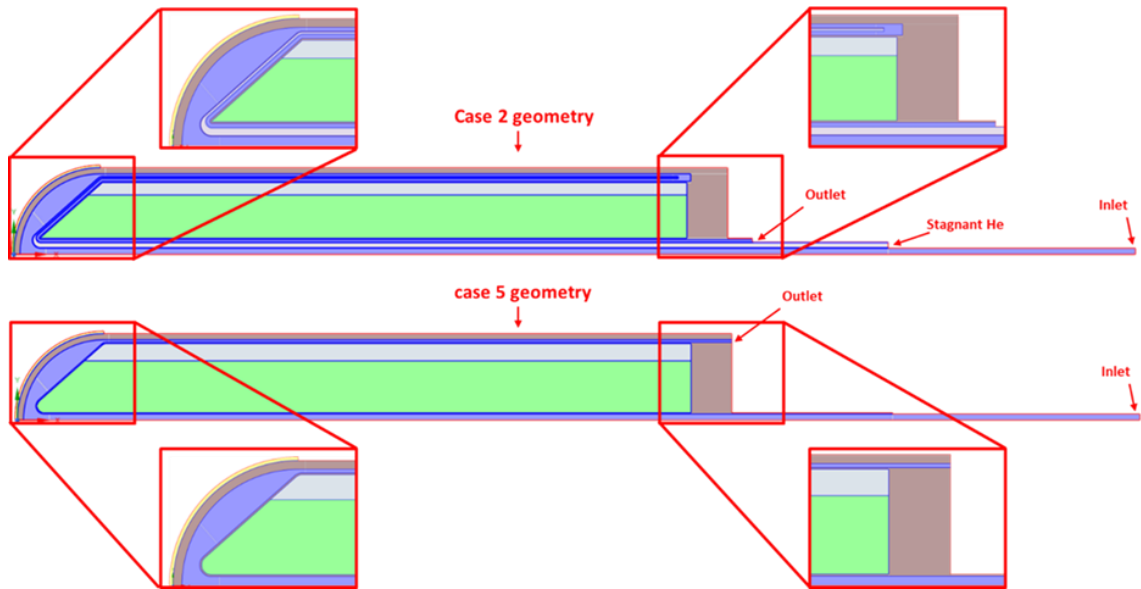


Figure 3.22: Case 5 geometry and comparison with case 2 layout.

Further interesting aspects of the case 5 pin architecture are the significantly increased volume of the BZ – potentially allowing a higher tritium production than the layouts with a double coolant flow pass – and the absence of the greatest pressure drop contribution shown in Table 3.20, constituted by the helium flow through the narrow outlet channel present in the double flow pass layouts.

The analyses on case 5 layout explored high roughness values (Table 2.9) on all the coolant-wetted surfaces adopting the Ansys High Roughness (Icing) model, and, accordingly, the mesh features of Table 3.2, which resulted in a mesh composed of $3.6 \cdot 10^5$ elements connected in $3.6 \cdot 10^5$ nodes. Once again, the analyses considered the HF_0 and ΔT values of Table 2.11 and Table 2.10 respectively.

The collected results obtained in the parametric analysis are extensively reported in Appendix B (Table B 11 to Table B 15). However, the outcomes of the analysis are presented hereby.

The case 5 layout is the first investigated geometry showing temperatures lower than 550 °C in all the Eurofer domains for at least one combination of parameters. Specifically, this occurs in two investigated cases, namely those considering the pin reference working conditions with roughness of 100 µm and of 200 µm respectively. The temperature field in the latter case is shown in Figure 3.23.

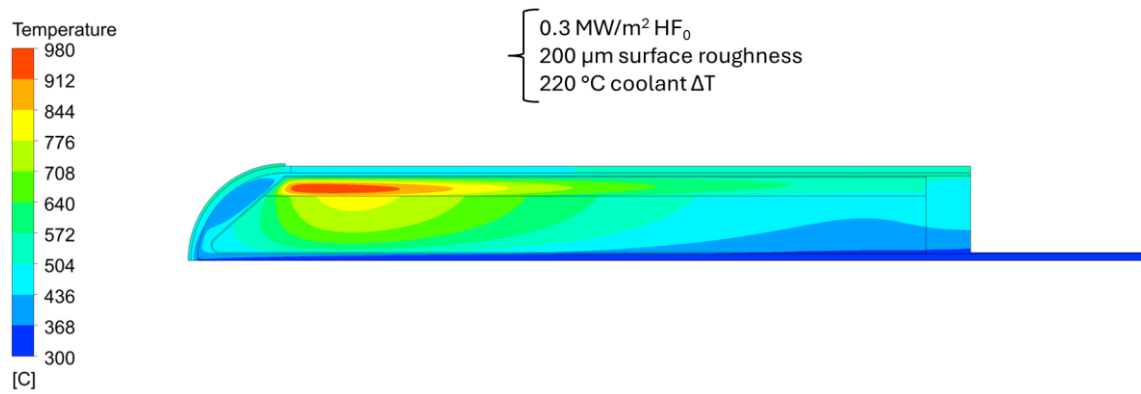


Figure 3.23: Temperature field in solid and fluid domains of case 5 layout.

It is to be mentioned that – as in most of the analysed cases – for the aforementioned combinations of parameters the case 5 layout exhibits maximum breeder temperatures slightly beyond the relevant design limit of 970 °C (Table 2.1). In more detail, the limit is overcome by 17 °C in reference working conditions with roughness of 100 µm and by 8 °C in the same conditions with roughness of 200 µm. However, this aspect is not deemed of particular concern because it is a very narrow violation that may be unphysical (the temperature field in the breeder is especially sensitive to the NH distribution, hence dedicated neutronic analyses on the HCPB monoblock may provide a more accurate distribution which does not cause the creation of the hotspot) or, if physical, can still be targeted by multiple strategies, including a slight modification in the arrangement of the functional materials within the BZ. Moreover, the case 5 layouts exhibits a few violations of the 100 m/s upper limit of coolant velocity in HCPB (2.2.2). In more detail, the threshold is exceeded in the inlet tube, but only in the cases with 1 MW/m² HF₀.

In order to gather further information on the potential of layouts envisaging a single coolant flow pass, it has been decided to perform a comparison between the case 5 and the case 2 layout – which is the layout envisaging a double coolant flow pass characterised by the optimal t_1 value – in relevant pin working scenarios. As a consequence, to provide an overall and comprehensive comparison between the two layouts, the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on surfaces “a”, “b”, and “c”, (i.e. in all coolant-wetted surfaces for the case 5) ΔT and HF₀ (data in Table B 2 to Table B 5 for case 2 and Table B 12 to Table B 15 for case 5) have been averaged and the relevant values are compared in Figure 3.24. In the results comparison, it is worth pointing out that the increase in the breeder and multiplier volume results in a +17% in the total deposited power in case 5 layout with respect to the case 2 geometry for HF₀ of 0.3 MW/m². Hence, the results shown correspond to significantly different flow velocities, with the case 5 layout having on average an inlet

helium flow velocity 8 m/s higher than the case 2. Despite that, the case 5 layout presents remarkably lower (over 25%) pressure drops than case 2, as it does not foresee the coolant flow in the small outlet channel of the case 2 layout, which is where the greatest pressure loss occurs in case 2 geometry (Table 3.20). Moreover, the comparison of the maximum temperatures in the cladding and cap shows the expected trend according to which the case 5 layout presents an improvement in the cladding thermal field at the cost of a substantial worsening in the cap thermal behaviour.

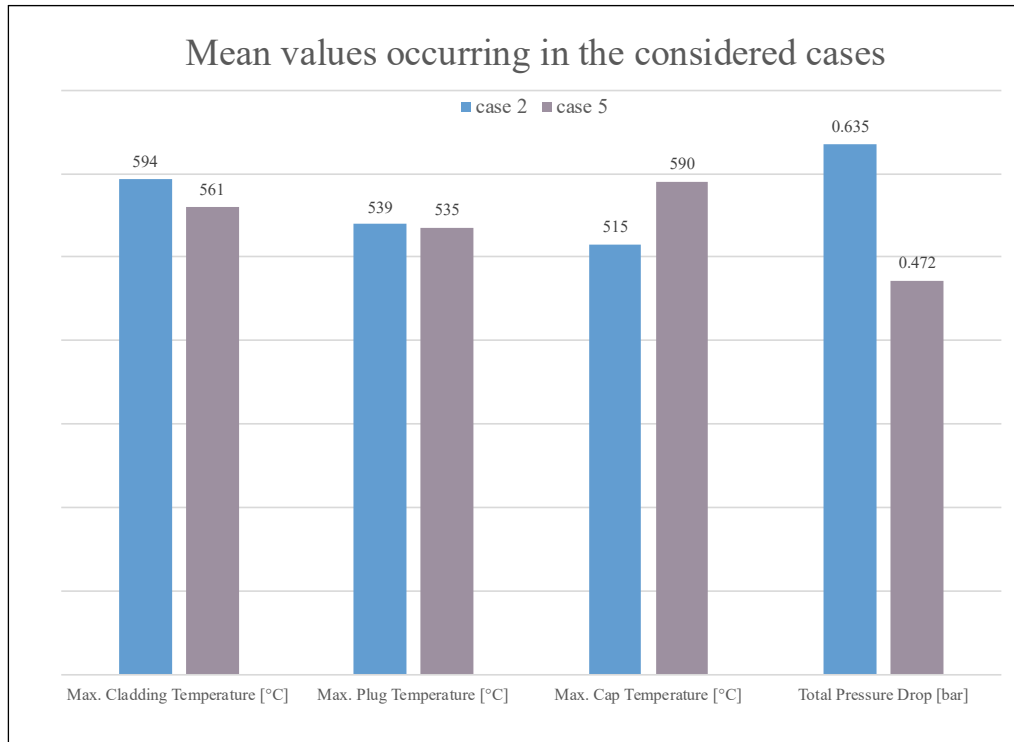


Figure 3.24: Comparison between case 2 and case 5 pin layout performance.

Based on the presented trends, it has been concluded that the layouts envisaging a double and a single coolant flow pass are both of interest for the HCPB monoblock pin design, as both configurations are, in reference working conditions, very close to complying with all the thermal and fluid-dynamic design requirements. Moreover, in both designs, the most critical areas allow wide margins of intervention (e.g., further material research for the Eurofer cladding of layouts with double coolant flow, or the possibility to change the BZ arrangement to remove the hotspot occurring in the breeder of single flow geometries). Finally, the two types of pin configuration are characterized by rather different advantages and drawbacks, which is an asset in view of the following phases of the HCPB monoblock design.

Once established that both the pin arrangements are worth further studies, the inlet tube radius has been identified as the following key-parameter to be analysed. In fact, this is the last portion of the pin layout to be investigated among the ones that has a major influence in

determining the coolant total pressure drop (recalling the pressure loss breakdown of Table 3.20) and the thermal field in critical regions. In more detail, a reduction of the inlet tube radius implies higher coolant velocities along the tube and across the jet impingement regions causing two effects: an increase of the coolant pressure loss and the enhancement of the fluid-solid heat transfer. In double coolant flow pass design, the inlet tube is thermally insulated by the stagnant helium layer, therefore the inlet tube radius can be expected to mainly affect the cap thermal behaviour. In single flow pass layouts, on the other hand, the coolant flowing in the inlet tube also interacts with the cladding, therefore, the inlet tube radius shall affect both the cap and cladding thermal field.

3.5.7 Parametric analysis on inlet tube radius r_1

The inlet tube radius r_1 has been identified as the last parameter to be investigated among the ones that significantly affect both the thermal and fluid-dynamic behaviour of the HCPB monoblock pin design. Considering the research outcomes presented in section 3.5.6, the parametric study on r_1 has been conducted both on single and double coolant flow pass pin configurations.

As to the layouts envisaging a double coolant flow pass, considering the results of the parametric analyses on the outlet channel thickness t_1 (section 3.5.3), the value of 2 mm has been kept fixed for t_1 . Furthermore, given the satisfactory thermal behaviour of the cap and the comparatively high pressure drop characterising such pin configurations, it has been retained of no interest to investigate r_1 values lower than 5 mm.

Although previously mentioned (section 2.3.4), the examined combinations of t_1 and r_1 values and the respective layout names are hereby reported for the sake of readability (Table 3.21). Moreover, Figure 3.25 shows the case 2 ($r_1 = 5$ mm) and case 12 ($r_1 = 8$ mm) geometries for further clarity.

Table 3.21: Analysed combinations of parameters t_1 and r_1 .

t_1 [mm]	2	2	2	2
r_1 [mm]	5	6	7	8
Name of the layout	Case 2	case 9	case 11	case 12

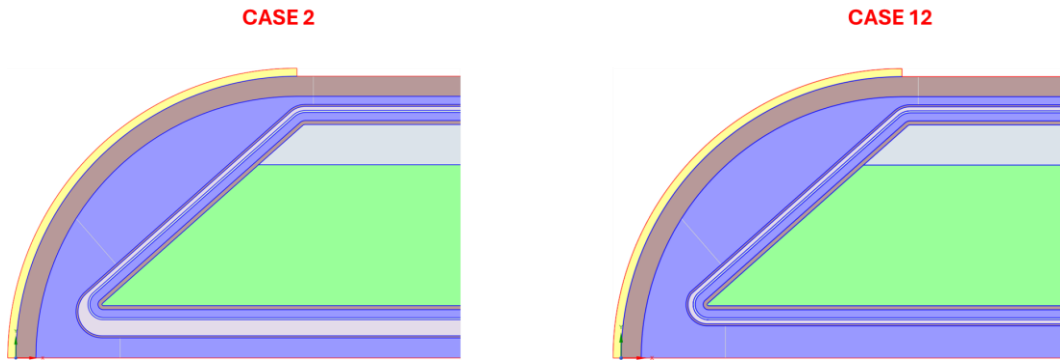


Figure 3.25: Apical area of case 2 geometry (left), and case 12 (right).

With the scope to obtain a reliable comparison between the investigated layouts, and to exploit the low computational burden characterising the analyses for gathering comprehensive data, also in the analysis of these geometries several combinations of loads and parameters have been investigated. In more detail, all the values of coolant temperature rise (Table 2.10) and plasma radiation load (Table 2.11) have been considered. Moreover, the analyses considered high roughness values (Table 2.9) on surfaces “a”, “b”, and “c”, adopting the Ansys High Roughness (Icing) model, and, accordingly, the mesh features of Table 3.2, which resulted in the mesh characteristics outlined in Table 3.22.

Table 3.22: Summary of discretization features for parametric analysis on r_1 on double coolant flow pass pin configurations.

Layout	N. of mesh nodes	N. of mesh elements
case 2	$8.8 \cdot 10^5$	$8.8 \cdot 10^5$
case 9	$8.8 \cdot 10^5$	$8.8 \cdot 10^5$
case 11	$8.7 \cdot 10^5$	$8.6 \cdot 10^5$
case 12	$8.6 \cdot 10^5$	$8.6 \cdot 10^5$

The collected results obtained in the parametric analysis are extensively reported in Appendix B (Table B 16 to Table B 20 for case 9, Table B 21 to Table B 25 for case 11, and Table B 26 to Table B 30 for case 12). However, the outcomes of the comparison between the design alternatives are hereby reported.

In order to provide an overall and comprehensive comparison between the examined layouts, the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on surfaces “a” and “b”, ΔT and HF_0 (data in Table B 2 to Table B 5 for case 2, Table B 17 to Table B 20 for case 9, Table B 22 to Table B 25 for case 11, and Table B 27 to Table B 30 for case 12) have been averaged and the relevant values are compared in Figure 3.26.

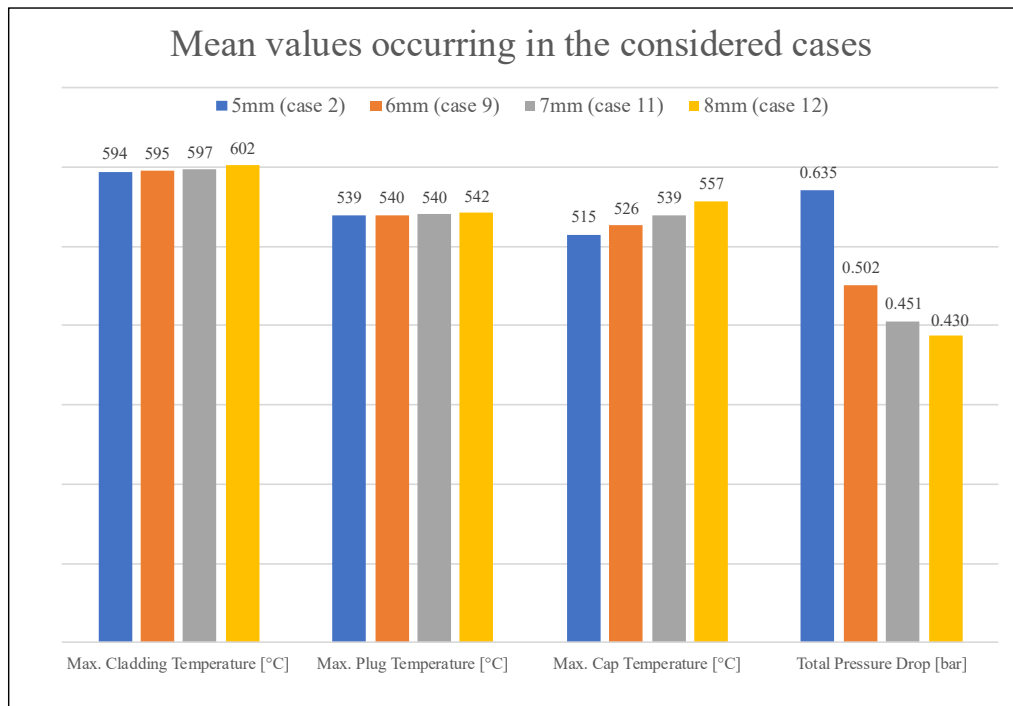


Figure 3.26: Comparison of the performance of the pin layouts with different r_1 and a double coolant flow pass.

Observing Figure 3.26 it can be noted that, as expected, increasing the inlet tube radius results in a lower coolant pressure drop, at the expense of higher Eurofer temperatures, especially in the cap. Moreover, the reduction of the coolant pressure drop is particularly relevant when passing from an inlet tube radius of 5 mm to a radius of 6 mm: this change allows for an average reduction of the pressure loss of over 20% at the expense of an average increase of maximum cap temperature by 11 °C. This result suggests that – although the case 2 layout remains the double coolant flow pass configuration with the lowest Eurofer temperatures – its r_1 value may not constitute the best trade-off between coolant pressure drop and Eurofer temperatures. The trend of the pressure drop also confirms that values of inlet tube radius lower than 5 mm are not of interest for double coolant flow pass layouts.

In this design stage, it is particularly difficult to pick a single optimised value for the inlet tube radius r_1 in the double coolant flow pass geometry, as the different alternatives present diverse advantages and drawbacks, the relative importance of which depends upon factors that are yet to be determined. Nevertheless, wider comparison and considerations are provided in section 3.5.9, presenting some final remarks on the thermofluid dynamic analysis campaign.

As to the layouts envisaging a single coolant flow pass, as anticipated, it has been retained of interest to investigate both r_1 values lower and higher than 5 mm. Although previously mentioned (section 2.3.4), the examined r_1 values and the respective layout names are hereby

reported for the sake of readability (Table 3.23). Moreover, Figure 3.27 shows the case 10 ($r_1 = 4$ mm) and case 14 ($r_1 = 7$ mm) geometries for further clarity.

Table 3.23: Examined r_1 values in single coolant flow pass pin configurations.

r_1 [mm]	4	5	6	7
Name of the layout	Case 10	case 5	case 13	case 14

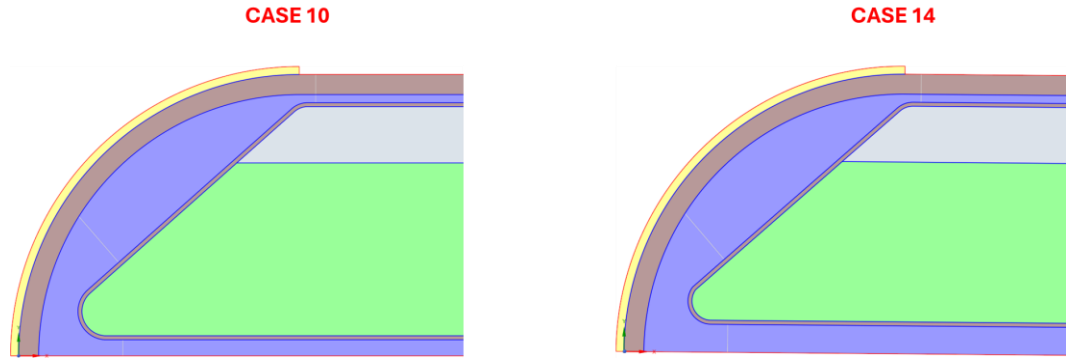


Figure 3.27: Apical area of case 10 geometry (left), and case 14 (right).

Once again, several combinations of loads and parameters have been investigated. In fact, all the values of coolant temperature rise (Table 2.10) and plasma radiation load (Table 2.11) have been considered. Moreover, the analyses considered high roughness values (Table 2.9) on all the coolant-wetted surfaces, adopting the Ansys High Roughness (Icing) model, and, accordingly, the mesh features of Table 3.2, which resulted in the mesh characteristics outlined in Table 3.24.

Table 3.24: Summary of discretization features for parametric analysis on r_1 in single coolant flow pass pin configurations.

Layout	N. of mesh nodes	N. of mesh elements
case 10	$3.5 \cdot 10^5$	$3.5 \cdot 10^5$
case 5	$3.6 \cdot 10^5$	$3.6 \cdot 10^5$
case 13	$3.6 \cdot 10^5$	$3.6 \cdot 10^5$
case 14	$3.7 \cdot 10^5$	$3.6 \cdot 10^5$

The collected results obtained in the parametric analysis are extensively reported in Appendix B (Table B 31 to Table B 35 for case 10, Table B 36 to Table B 40 for case 13, and Table B 41 to Table B 45 for case 14). However, the outcomes of the comparison between the design alternatives are outlined hereby.

In reference working conditions and with surface roughness of $200 \mu\text{m}$, all the layouts outlined in Table 3.23 comply with the upper Eurofer temperature design limit of 550°C except the case 14 geometry, which exceeds the limit by 11°C in the Eurofer cladding. In order to provide an overall and comprehensive comparison between the examined layouts,

the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on surfaces “a” and “b”, ΔT and HF_0 (data in Table B 32 to Table B 35 for case 10, Table B 12 to Table B 15 for case 5, Table B 37 to Table B 40 for case 13, and Table B 42 to Table B 45 for case 14) have been averaged and the relevant values are compared in Figure 3.28.

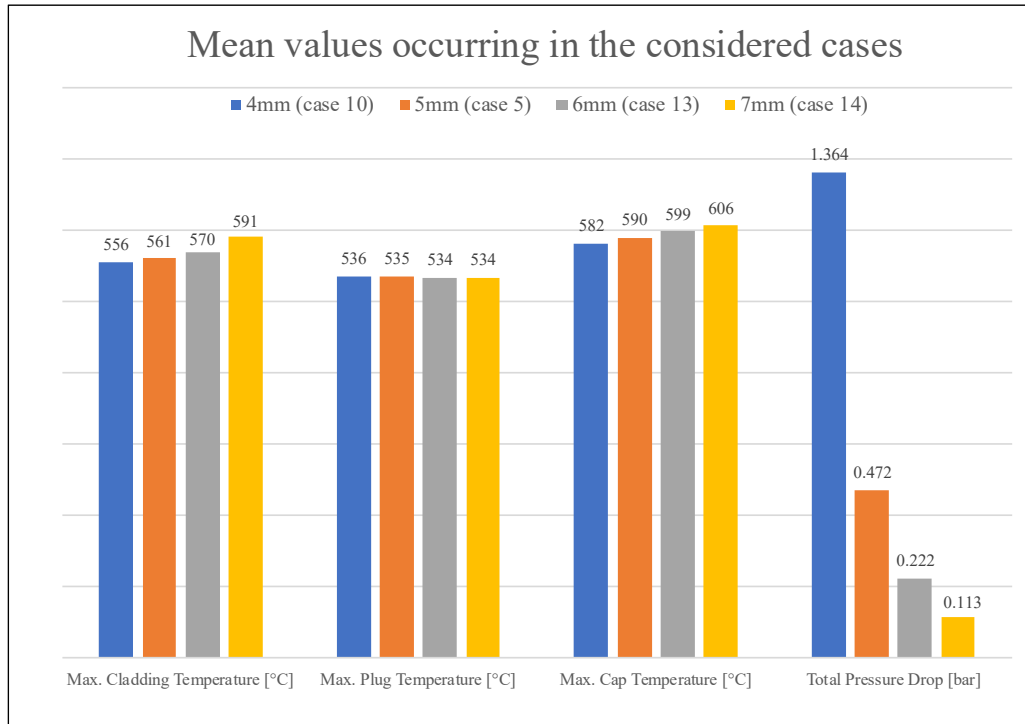


Figure 3.28: Comparison of the performance of the pin layouts with different r_1 and a single coolant flow pass.

Observing Figure 3.28, it is particularly noticeable the abrupt increase in the total pressure drop that occurs when passing from the case 5 to the case 10 layout, which exhibits pressure drops beyond the design limit of 0.8 bar in almost all of the considered scenarios. Considering the comparatively low temperature reductions achieved by the case 10 layout in solid material domains, this pressure loss behaviour allows to rule out definitively the opportunity to reduce the inlet tube radius of the HCPB monoblock below 5 mm. Moreover, the inlet tube of the case 10 layout is the smallest flow cross section present in any of the considered layouts, and presents violations of the upper coolant velocity limit (100 m/s) in all the analysed cases except than for a coolant temperature rise of 300 °C.

On the other hand, as it happens in the layouts with a double coolant flow pass, also in the layouts with a single pass significant reductions of the total pressure drop are observed when increasing the inlet tube radius beyond 5 mm. Moreover, in this case, the effect is more marked in terms of average percentual variation due to the absence of the important pressure loss contribution in the small outlet channel of double coolant flow pass layouts (Table 3.20).

As a result, the average pressure drop is reduced by over 50% by increasing the inlet tube radius from 5 mm (case 5) to 6 mm (case 13), at the cost of an average increase of 9 °C of both the maximum cladding and cap temperatures. Also in this case, this evidence suggests that, although the case 5 geometry remains the single coolant flow pass layout with the lowest Eurofer temperatures, its r_1 value may not constitute the best trade-off between coolant pressure drop and Eurofer temperatures, and a broader discussion on the optimal r_1 is presented in section 3.5.9.

Finally, attention has been focused on the possibility to change the thickness of the outer annular channel contiguous to the shell, which is object of the following section.

3.5.8 Investigation on outer annular channel thickness t_2

The outer annular channel is the last main zone of the coolant flow path to be investigated.

For as regards the pin architectures envisaging a double coolant flow pass, changing the thickness of the outer annular channel t_2 (Figure 3.29) does not appear to be an effective strategy to improve the pin's thermal and fluid-dynamic behaviour.

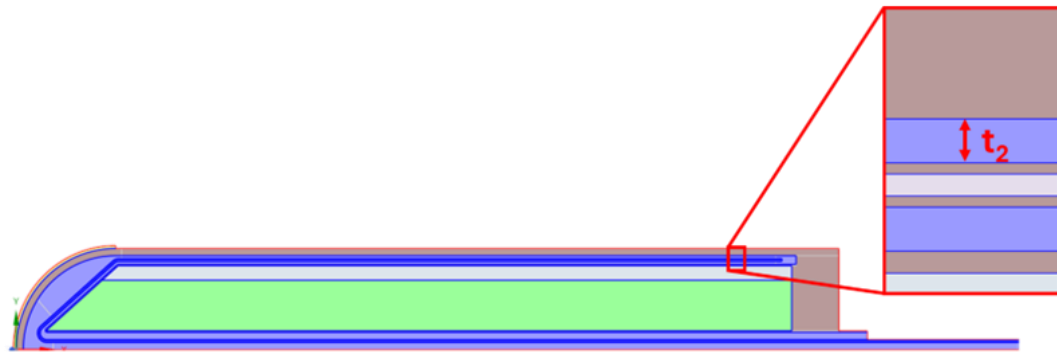


Figure 3.29: Illustration of outer channel thickness t_2 in double flow pass layouts.

In fact, considering that the pressure loss occurring in the outer annular channel is a negligible share of the total pressure drop (Table 3.20) occurring in these pin architectures (when considering, obviously, the layouts with the optimized thickness of the outlet annular channel t_1 of 2 mm), enlarging the outer annular channel (increasing t_2 beyond 2 mm) would have the negative effect of reducing the flow speed in the proximity of the shell – thus reducing the effectiveness of shell cooling – without, in turn, leading to any significant advantage in terms of pressure drop. On the other hand, also reducing the outer annular channel thickness does not seem an effective strategy in the quest for a better trade-off between thermal field in solid domains and coolant total pressure drop. In fact, a reduction of t_2 would lead to an increased pressure loss occurring in the outer annular channel, and to enhanced solid-fluid heat transfer in the shell. However, the double coolant flow pass pin architectures exhibit a rather satisfactory thermal behaviour of the shell, while having the

drawback of generally higher coolant pressure drops than the single pass options. Hence, the reduction of t_2 would lead to a less desirable overall thermal and fluid-dynamic behaviour.

An important remark to be done is that, despite being negligible the pressure loss occurring in the outer channel in double coolant flow pass architectures with a t_2 of 2 mm, however, the pressure drops in the same region can become relevant and also high when reducing t_2 . In fact, recalling the behaviour observed when reducing t_1 below 2 mm, and r_1 below 5 mm, it can be intuited that also reducing t_2 can lead to a rather abrupt increase in the pressure losses. To gain quantitative information on this behaviour, the normalized pressure loss occurring when reducing t_2 from 2 mm to 0.5 mm by steps of 0.5 mm has been evaluated by means of Eq. 3.30 and is reported in Table 3.25.

Table 3.25: Estimated normalized pressure loss in outer annular channel for different thickness values.

t_2 [mm]	2	1.5	1	0.5
Estimated normalized pressure loss	1	2.35	7.88	62.53

For this reason, in the speculation above, the beneficial effect of increasing t_2 on the total pressure drop has been deemed irrelevant, while the negative effect caused by a reduction of t_2 on the total pressure drop has been taken into consideration.

One final remark is that the above explanation perfectly holds when t_2 is modified by changing the thickness of the stagnant helium layer in the outer channel region. However, in double coolant flow pass layouts, in principle, it is possible to let t_2 vary also in different ways, such as by changing the BZ volume, by changing the gap between the cladding and inner sheet, or by combining these effects. However, when analysing these scenarios in light of the dissertation presented above, it becomes clear that the logical steps to assess whether or not a scenario is appealing only change slightly, but the overall conclusion remains the same. A wider discussion of the effects obtained by modifying t_2 in every different way, hence, does not really provide any further insight into the thermofluid-dynamics of the HCPB monoblock, nor any scientifically relevant consideration, and therefore is not hereby addressed. It can be stated that the baseline configuration of the outer region of the coolant flow path in double pass geometries provides a satisfactory thermal and fluid-dynamic behaviour, by guaranteeing effective cooling of the interested regions with a negligible pressure loss. Therefore, no significant improvement of the thermal and fluid dynamic behaviour of these geometries can be achieved by focusing on this region.

When considering the effect of t_2 on the thermal and fluid-dynamic performance of single flow pass layouts, some substantial differences need to be considered. First, it should be

noticed that, although the pressure loss contribution occurring in the outer annular channel is negligible when t_2 is of 2 mm in optimized double coolant pass layouts, the same should not be automatically assumed for the single pass geometries. In fact, the latter have lower pressure drops and, most importantly, do not feature the inner annular channel, that is where the greatest share of pressure loss occurs (Table 3.20) in optimized double flow pass geometries. Secondly, it has to be considered that, due to the absence of the stagnant helium layer, in single flow pass geometries t_2 can only be changed by changing the BZ volume. Hence, increasing the thickness of the outer annular channel in single flow pass layouts results in a smaller BZ and, under the assumption of fixed neutronics, a lower deposited power. Consequently, for a given coolant temperature rise, a lower coolant MFR is imposed, which implies lower coolant velocities and, finally, a less effective cooling of the cap. Moreover, a more pronounced reduction of flow velocity occurs in the outer annular channel, where the coolant velocity is reduced for the combined effect of the reduced MFR and the wider flow section. Hence, a less effective cooling of the shells is also expected. In turn, a relevant reduction of the pressure drop should be achieved by virtue of the twofold effect of the lower MFR and of the higher flow section in the outer annular channel. It can be noted that the main changes expected by increasing t_2 on the thermal and fluid-dynamic behaviour of the pin are not in the direction of a better trade-off between structural material temperatures and pressure drop. In fact, single coolant flow pass geometries already assure a rather satisfactory pressure drop, while presenting the downside of comparatively high temperatures occurring in the cap and shell. However, for the same considerations, it has been retained of interest to analyse a single flow pass layout featuring a reduction of the outer channel thickness t_2 .

Since an increase in the total pressure drop is expected, it has been decided to test a reduction of 1 mm starting from the case 13 geometry ($r_1 = 6$ mm), selected by virtue of the behaviour visible in Figure 3.28, that shows a significantly lower pressure drop than the case 5 geometry ($r_1 = 5$ mm), without dramatic increases in the structural material temperatures. Hence, the case 15 geometry has been created by reducing the outer annular channel thickness of the case 13 layout from 2 mm to 1 mm.

The analyses on case 15 layout explored high roughness values (Table 2.9) on coolant-wetted surfaces adopting the Ansys High Roughness (Icing) model, and, accordingly, the mesh features of Table 3.2, which resulted in a mesh composed of $3.9 \cdot 10^5$ elements connected in $3.9 \cdot 10^5$ nodes. Once again, the analyses considered the HF_0 and ΔT values of Table 2.11 and Table 2.10 respectively.

The collected results obtained in the parametric analysis are extensively reported in Appendix B (Table B 46 to Table B 50). However, the outcomes of the analysis are illustrated hereby.

It has been retained of interest to perform a comparison between the thermal and fluid-dynamic behaviour of the case 13 and the case 15 layouts (see Table 3.26 and BBB), so to identify the effects of the variation of t_2 on the performance of a single flow pass layout.

Table 3.26: Geometric features of the case 13 and case 15 layout.

Name of the layout	case 13	case 15
t_2 [mm]	2	1
r_1 [mm]	6	6
Coolant flow pass	single	single

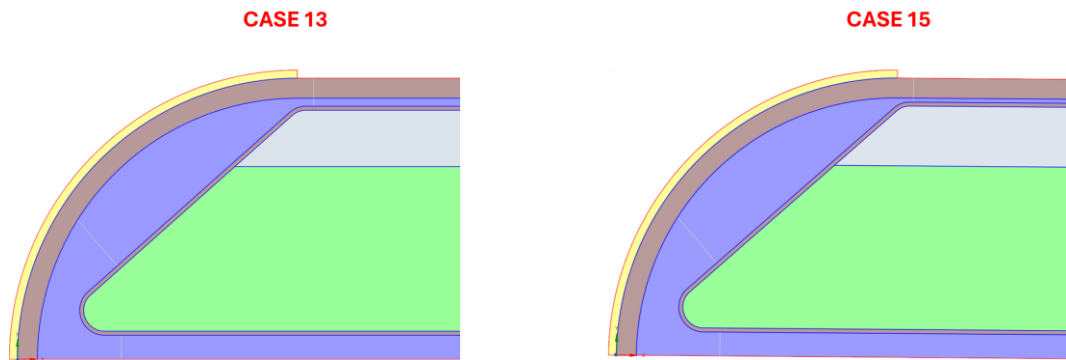


Figure 3.30: Apical area of case 13 geometry (left), and case 15 (right).

In order to provide an overall and comprehensive comparison between the two layouts, the maximum temperatures occurring in three Eurofer domains and the coolant total pressure drop occurring in the analyses with varying roughness on coolant wetted surfaces, ΔT and HF_0 (data in Table B 37 to Table B 40 for case 13 and Table B 47 to Table B 50 for case 15) have been averaged and the relevant values are compared in Figure 3.31.

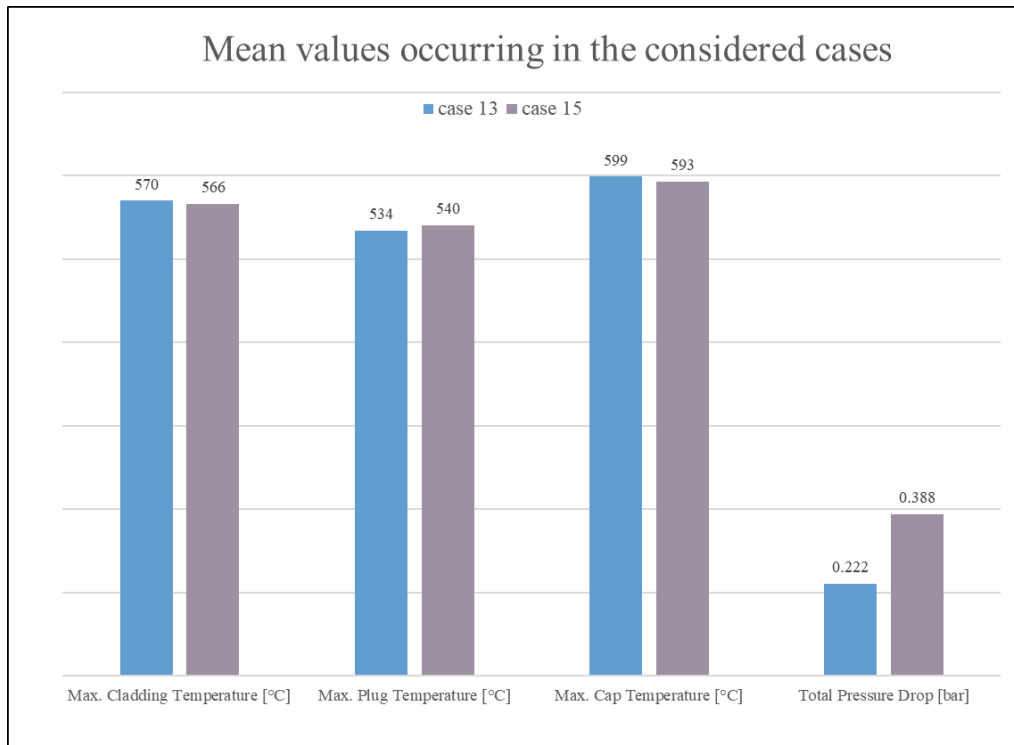


Figure 3.31: Comparison between case 13 and case 15 pin layout performance.

The results comparison shows that, as expected, the pressure drop rises significantly in single flow pass layouts when reducing t_2 from 2 mm to 1 mm. In fact, the case 15 geometry exhibits a pressure drop that is, averaging in the considered cases, 75 % higher than that of the case 13 layout. Considering the data shown in Table 3.25, it is expected that further reductions of t_2 would yield unduly high pressure losses. Moreover, as predicted, lower temperatures in the cap are achieved, although the reduction is moderate. Interestingly, also cladding temperatures are reduced, showing that in this domain the effect of enhanced heat fluid-solid heat transfer prevails on the effect of the higher deposited power in the BZ. In general terms, the case 15 geometry shows a behaviour that is intermediate between that of the case 5 geometry ($r_1 = 5$ mm, $t_2 = 2$ mm), that has lower Eurofer temperatures and a higher pressure drop, and that of the case 13, that shows higher Eurofer temperatures and a lower pressure drop. However, the case 15 layout presents a criticality that does not arise in either case 5 or case 13. In fact, when checking the breeder temperatures, it emerges that, due to the wider breeder volume, the temperature design limit of 970 °C (Table 2.1) is overcome in every considered scenario by a significant margin. To provide an example, under pin reference working conditions and with a roughness value on coolant-wetted surfaces of 200 μ m, the limit is overcome by 43 °C, while the homologous violation for the case 13 layout is by only 7 °C. Although results in this region should not be retained highly reliable because heavily affected by the actual neutronics, still this dramatic change seems to indicate that the dimensions of the BZ start exceeding the concept's cooling capability.

Hence, claiming that the case 15 layout ensures the compliance with all the HCPB monoblock temperature design limits may be a too optimistic statement.

Considering this criticality, along with the fact that the thermal and fluid-dynamic performance of case 15 is not particularly appealing compared to the case 5 and case 13 configurations, case 15 has not been considered among the most promising layouts in the thermofluid-dynamic optimization, and the opportunity to improve the thermofluid-dynamics of the HCPB monoblock by intervening on the outer channel thickness t_2 has been definitively ruled out.

With the investigation on the outer channel thickness, the phase of collecting data on the thermofluid-dynamic behaviour of the HCPB monoblock pin has been concluded. Some wider comparisons of the analysed pin layouts are presented in section 3.5.9, and, finally, the conclusions of this study are outlined in section 3.6.

3.5.9 Final remarks on the thermofluid-dynamic analysis campaign

The parametric analysis on the outlet channel radius r_1 detailed in section 3.5.7 proved that adopting lower radii than the baseline (5 mm) is detrimental to the performance of the pin, but also depicted a complex picture of multiple layouts with higher radii exhibiting a satisfactory thermal and fluid-dynamic behaviour. The sole objectives and constraints of the thermal and fluid dynamic optimisation are not sufficient to pick specific values for the optimal r_1 of single and double coolant flow pass configurations, as well as selecting a single and definitive optimized pin layout is not feasible until further information is available (e.g., outcomes of research on cladding materials, prioritized heat loading scenarios).

Given the complexity of the picture, it has been retained worth to present – in this section – some wider speculation on the role of the inlet tube, and some broader discussion on the promising pin layouts that emerge from the thermofluid-dynamic analysis campaign.

In a double coolant flow pass geometry, the inlet tube is thermally isolated by means of the surrounding stagnant helium layer. As a consequence, high coolant velocities in the tube of such configurations are solely detrimental to the pin performance, as they increase the pressure loss without, in turn, improving the thermal behaviour of any critical domain. High fluid velocities become beneficial only when the coolant reaches the jet impingement area, where they enable effective cooling of the Eurofer cap. This observation may drive the designer towards considering a reshaping of the inlet tube and the jet impingement in such a way to have an higher radius along the tube (reducing coolant flow speed and thus pressure drop) and to have a restriction with consequential increase of helium velocities in the proximity of the cap (endowing enhanced heat transfer and thus lower temperatures in the

cap). Although conceptually sound, this solution has not been pursued due to the trends shown in Figure 3.26, which indicate that increasing the inlet tube radius beyond 6 mm yields only marginal reductions in pressure loss. Consequently, a furtherly “optimized” layout combining the low pressure drop of a configuration with r_1 of 9 mm and the improved cap thermal performance typical of r_1 of 5 mm (or potentially better, using a more pronounced restriction) does not seem preferable than the case 9 layout. In fact, the latter configuration, despite having slightly higher pressure drop and cap temperatures (yet widely within the respective design limits) offers a considerably simpler geometry, with consequential benefits in terms of manufacturing cost and thermomechanical behaviour.

On the other hand, this solution is far less appealing for a single coolant flow pass layout, as in this case high velocities in the inlet tube have the beneficial effect of enhancing the cooling of the Eurofer cladding and the BZ. Moreover, introducing a restriction in the inlet tube would lead to greater manufacturing challenges, as it would require bending the Eurofer cladding and adopting a more complex shape for the neutron multiplier.

Therefore, the studies on inlet tube radius r_1 and on jet impingement shape have not progressed beyond what is presented in sections 3.5.5 and 3.5.7. However, for the sake of completeness, some wider consideration and comparison between the viable layouts with different r_1 that emerged at the end of the thermofluid dynamic analysis campaign is hereby presented. To this end, a total of six layouts has been considered, of which three foresee a single coolant flow pass (namely case 5, case 13 and case 14) and three a double coolant flow pass (case 2, case 9 and case 12). Among the geometries presented in section 3.5.7, the case 10 layout was ruled out due to its poor thermofluid-dynamic behaviour, while the case 11 layout was only excluded because, presenting an intermediate behaviour between the case 9 and the case 12, its inclusion would reduce the clarity of the graphs without providing additional points for consideration. The mentioned layouts have been compared considering a 200 μm roughness on machinable surfaces, and a coolant temperature rise of 220 $^{\circ}\text{C}$, as both these conditions emerged as especially viable in the performed analyses. Once again, the maximum temperatures occurring in three Eurofer domains and the total pressure drop have been selected as variables to compare among the different pin designs. The values of these indicators are shown in Figure 3.32 considering the reference HF_0 value of 0.3 MW/m^2 , in Figure 3.33 considering an HF_0 of 0.48 MW/m^2 , and in Figure 3.34 considering an HF_0 of 0.65 MW/m^2 .

In general terms, it can be appreciated that the different pin layouts exhibit a variety of diverse thermal and fluid dynamic behaviours complying with the design constraints, which is a sign that the HCPB monoblock concept has the potential to offer working solutions

tailored to future requirements and research developments that may emerge during the DEMO design.

Observing the trends, it is interesting to notice how the double coolant flow pass configurations with inlet radii greater than 5 mm (namely case 9 and 12 in the figures) have coolant pressure drops that are comparable to that of case 5 layout, envisaging a single coolant flow pass and an inlet radius of 5 mm. Similarly, it is relevant to point out that the case 13 layout – characterised by a single coolant flow pass and an inlet tube radius of 6 mm – marks a significant drop in the coolant pressure loss compared to the case 5 layout and the double coolant flow pass geometries.

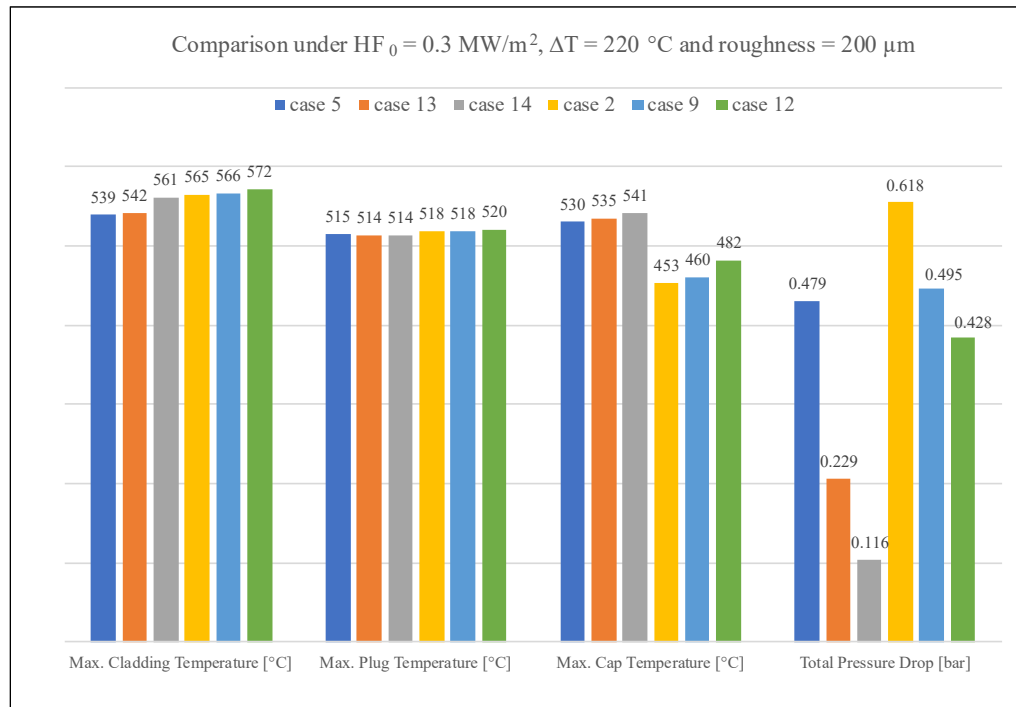


Figure 3.32: Comparison of the performance of six promising pin layouts in reference working conditions.

When considering the scenario characterised by HF_0 of 0.3 MW/m^2 , the case 13 layout is the one with the lowest pressure drop to ensure Eurofer temperatures below $550 \text{ }^\circ\text{C}$. Moreover, in these conditions, the geometries with a single coolant flow pass also seem more appealing than the ones with a double coolant pass because they have significantly higher BZ volumes – potentially leading to markedly higher TBRs – and a remarkably simpler design, generally leading to higher concept reliability and less demanding manufacturing. Should the scenario with HF_0 of 0.3 MW/m^2 be confirmed as a reference, and should cladding temperatures below $550 \text{ }^\circ\text{C}$ be deemed a design requirement, observing Figure 3.32 the case 13 layout would constitute the best choice. Nevertheless, in the same scenario, should cladding temperatures over $550 \text{ }^\circ\text{C}$ be acceptable, the case 14 may be preferred by virtue of its remarkably low pressure drop. Additionally, in this scenario, should the high

(even though below 550 °C) cap thermal temperatures cause a poor thermomechanical behaviour of the single coolant flow pass layouts, a double coolant pass layout as the case 9 may be evaluated as a trade-off solution with a higher pressure drop and lower structural material temperatures. Finally, it can be observed that all layouts comply with the upper limit of 0.8 bar for the total pressure drop. Consequently, the case 5 and case 2 layouts emerge as the geometries with the lowest Eurofer temperatures – among the single and double coolant flow pass layouts, respectively – that satisfy the pressure drop design constraint.

On the basis of this, and due to the impossibility to establish an optimal layout with the available information, the case 5 and case 2 geometries have been selected for the exploratory thermomechanical study on the HCPB monoblock. This choice allows to perform the studies in the most favourable conditions from the thermomechanical standpoint, thus enabling to draw conclusions on the structural viability of the HCPB monoblock concept.

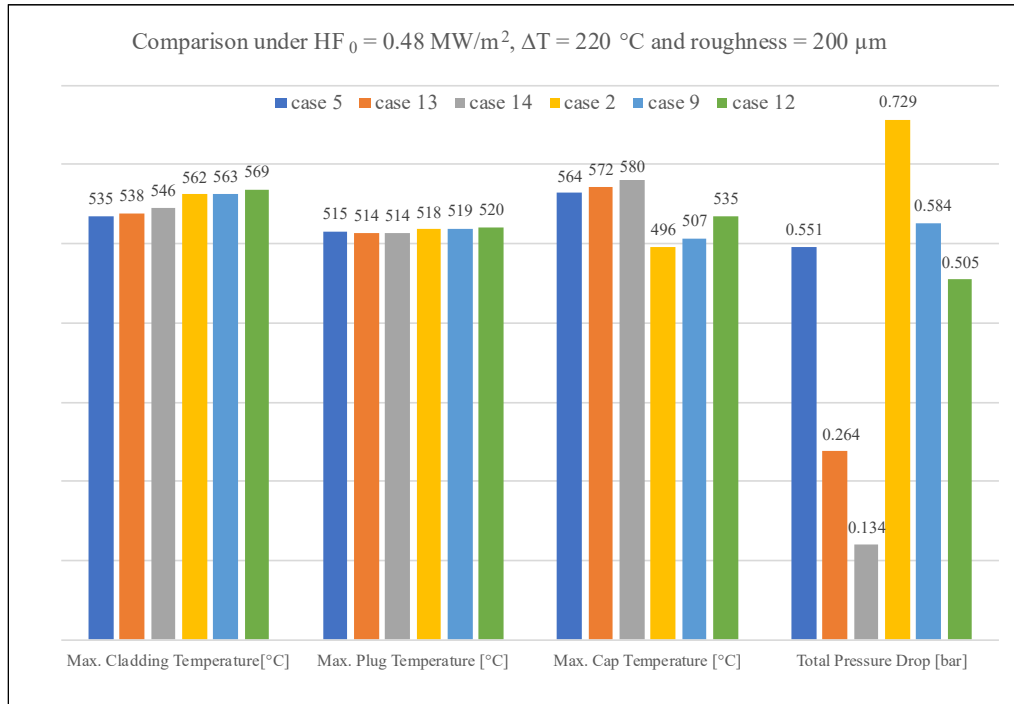


Figure 3.33: Comparison of the performance of six promising pin layouts under HF_0 of 0.48 MW/m^2 .

When considering the scenario characterised by HF_0 of 0.48 MW/m^2 , it can be noted that all the geometries with a single coolant flow pass exhibit cap temperatures exceeding the $550 \text{ }^\circ\text{C}$ design limit (Figure 3.33). Should 0.48 MW/m^2 be chosen as a scenario of particular interest and the $550 \text{ }^\circ\text{C}$ limit strictly enforced, this would rule out all the single flow pass designs. Among the remaining layouts, the case 2 would probably be excluded due to the high pressure loss that is approaching the limit of 0.8 bar. Finally, between the case 9 and case 12 layouts, if the higher cap temperature of the latter does not lead to poor

thermomechanical performance, the latter would be the preferable design due to its lower pressure drop. Conversely, if the thermomechanical behaviour is compromised by the high cap temperatures, the case 9 geometry would be the more suitable choice.

Finally, the scenario characterised by HF_0 of 0.65 MW/m^2 (Figure 3.34) highlights unduly high cap temperatures in the single coolant flow pass pin arrangements and in the case 12 geometry, and an excessively high (beyond 0.8 bar) total pressure drop of the case 2 layout. However, it is impressive that even in such a harsh thermal loading scenario, there is still one layout, namely the case 9 geometry (envisaging a double coolant flow pass and an inlet radius of 6 mm) that exhibits a pressure loss within the design limit while also being extremely close to compliance with the upper temperature limit for Eurofer. Although the thermomechanical behaviour in such prohibitive conditions would have to be evaluated, still this result emphasizes that having diverse valid design options for the architecture of the pin internals of the HCPB monoblock constitutes a strategical asset in view of more advanced stages of the DEMO design, where certain characteristics of either pin arrangement may prove to be game changers.

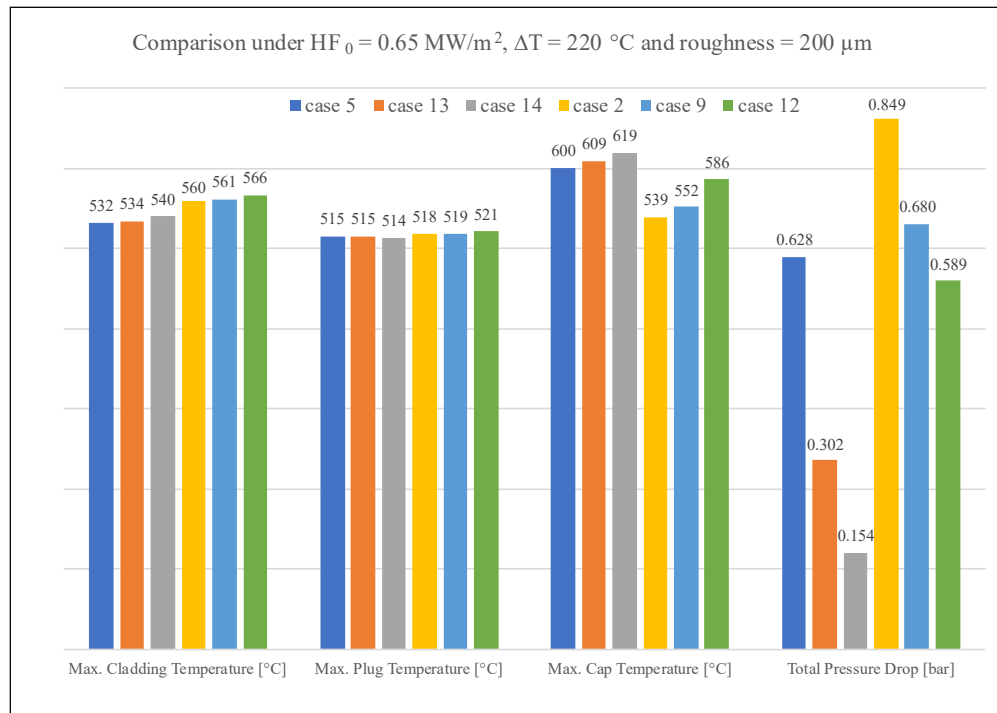


Figure 3.34: Comparison of the performance of six promising pin layouts under HF_0 of 0.65 MW/m^2 .

3.6 Conclusions of the thermofluid-dynamic analysis

The thermofluid-dynamic optimisation of the HCPB monoblock pin layout has considered a wide range of geometrical parameters, studying the performance of several pin geometries under different loading conditions and with different values of roughness on machinable surfaces.

It has been found that coolant temperature rise values over 220 °C imply prohibitive structural material temperatures and may be viable only in double coolant flow pass architectures featuring a cladding of a different material different from Eurofer. Moreover, it has been discovered that machining on coolant-wetted surfaces helps significantly in lowering maximum Eurofer temperatures, and that viable working conditions with HF_0 values over 0.3 MW/m² can be found, although when approaching 0.8 MW/m² cap temperatures always become unduly high.

A preliminary comparative analysis has shown that reducing the outlet channel thickness t_1 below the 5 mm of the baseline layout leads to better pin performances when accomplished by enlarging the stagnant helium layer rather than the breeding zone.

From the succeeding parametric study on the outlet channel thickness t_1 of double coolant flow pass geometries, the value of 2 mm clearly emerged as the best trade-off, allowing substantially lower cladding temperatures than 3 mm, and a dramatically lower pressure drop than 1 mm.

Subsequently, it has been found that layouts envisaging a single flow pass along the pin domain can achieve Eurofer temperatures below 550 °C under nominal working conditions, while also benefitting from a significantly wider BZ (thus, supposedly, higher TBR) and a simpler design.

Afterwards, parametric studies on the inlet radius r_1 have been conducted on both pin arrangements, proving that adopting lower radii than the baseline (5 mm) is detrimental. A more complicated picture emerges when considering higher radii than the baseline, as multiple layouts present a satisfactory thermal and fluid dynamic behaviour. As a general rule, single coolant flow pass layouts seem to outperform the double coolant pass architectures when considering the reference HF_0 value of 0.3 MW/m², but their performance rapidly deteriorates when subjected to more intense radiation loads, which are handled much more effectively by double coolant pass configurations. However, a final decision on the best r_1 value and on the optimal pin configuration cannot be drawn prior to the outcomes of further research on cladding materials, further thermomechanical analyses, and the specification of prioritized heat loading scenarios.

Finally, a theoretical-numerical investigation on the outer annular channel thickness t_2 has shown that no significant improvement can be achieved by varying this parameter.

The case 5 and case 2 layouts emerge as the geometries with the lowest Eurofer temperatures – among the single and double coolant flow pass layouts, respectively – that satisfy the pressure drop design constraint. Hence, these pin architectures have been selected

for the exploratory thermomechanical detailed in Chapter 4 aimed at evaluating the structural viability of the HCPB monoblock concept.

A final remark on the thermofluid-dynamic analysis campaign is that strengths and weaknesses of single and double coolant flow pass layouts appear to be somewhat complementary. Additionally, the choice of an inlet radius value (equal or higher than 5 mm) provides a further degree of freedom in the design of the HCPB monoblock pin. As a result, multiple layout options with diverse and valid thermal and fluid-dynamic performances are available, which endeavours the concept with “resilience” to future findings and specifications that may emerge in more advanced stages of the DEMO design.

Chapter 4

Thermomechanical analysis of analysis of a pin of the HCPB monoblock BB

4.1 Introduction

The final part of the work carried out during the PhD years focused on the exploratory thermomechanical assessment of a pin of the HCPB monoblock located in the COB blanket segment, with the purpose to investigate the concept's viability from the structural standpoint. The thermofluid-dynamic analysis campaign detailed in Chapter 3 has revealed that different pin design options comply with the HCPB monoblock thermal and fluid-dynamic constraints. For the thermomechanical assessment, it has been decided to analyse one pin geometry characterised by the double coolant flow pass and one layout envisaging a single flow pass, given the major geometrical and thermal differences between the two configurations. In more detail, the case 2 and the case 5 layout have been selected, by virtue of being the two layouts exhibiting the lowest Eurofer temperatures with an allowable coolant pressure drop. Moreover, the structural response has been assessed considering, for both the pin geometries, the respective thermal fields relevant to the reference working conditions, with roughness on machinable surfaces of 200 μm .

The thermomechanical analysis has been conducted following a theoretical-numerical approach based on the Finite Element Method (FEM) and adopting the Ansys Mechanical 2023 R2 [20] commercial Finite Element Analysis (FEA) code. Moreover, a multi-scale approach has been adopted, assessing, in a first phase, the global thermo-mechanical performances of the equatorial region of the Central Outboard Blanket segment moving, in a second phase, to the detailed structural analysis of the internals of a single pin. The obtained stress fields have thus allowed to perform the stress linearization necessary to carry out the structural verification attending the rules prescribed by the RCC-MRx structural design code [19].

In the following, the methodology, setup, and results of the thermomechanical assessment are described and critically discussed.

4.2 Description of the methodology

To perform the thermomechanical analysis of a generic component, it is necessary to determine both its thermal and mechanical states, based on the applied loads, boundary conditions and suitable assumptions. While the thermal state is directly linked to the

temperature distribution, the mechanical state requires knowledge of the stress, strain, and displacement fields [66].

In general cases, the determination of the thermal and of the mechanical states are coupled, which originates great difficulties in the resolution. However, in the present studies, it can be assumed that the HCPB monoblock pin structural deformations do not significantly affect its temperature field. This assumption allows to proceed with the determination of the thermal state and, subsequently, to consider it as one of the stress terms for the assessment of the mechanical state. Hence the two problems can be target separately and subsequently, with a considerable reduction of the analytical burden without any significant loss of results accuracy and reliability.

As to the thermal state, the temperature distribution within the HCPB monoblock pin domain has been calculated through the CFD analyses detailed in Chapter 3 . However, to perform more accurate structural evaluations on the pin, it has been useful to extrapolate the thermal field in the wider domain of an HCPB monoblock COB segment. Hence, as it happened for the previously detailed CFD computations, also in the analyses presented in this chapter, the heat balance equation in solids has been solved (Eq. 3.5). As already mentioned, Eq.3.5 can only be solved in analytical form in cases characterised by especially simple combinations of geometry, loads, and BCs, therefore it is common to recurse to numerical methods for determining an approximate solution. However, whilst the governing equation had been formerly solved on the pin solid domains through an FVM based computation, a FEM based calculation has been carried out to extrapolate the thermal repartition on the HCPB monoblock COB segment.

As to the mechanical state, its determination is a complex problem that is typically targeted, in the engineering practice, using the assumptions of the Theory of Elasticity([66], [67]), namely the occurrence of small displacements and infinitesimal deformations within a structure that is supposed to behave as a solid continuum. Hence, the problem translates into the resolution of a complex system of algebraic-differential equations with partial derivatives consisting of the equilibrium, compatibility, and constitutive equations [66].

The equilibrium equations enforce translational (Eq. 4.1) and rotational (Eq. 4.2) equilibrium (null linear and rotational acceleration) of any elementary volume composing the structure, that is subjected to external loads:

$$\begin{cases} \frac{\partial \sigma_x}{\partial x} + \frac{\partial \tau_{yx}}{\partial y} + \frac{\partial \tau_{zx}}{\partial z} + F_x = 0 \\ \frac{\partial \tau_{xy}}{\partial x} + \frac{\partial \sigma_y}{\partial y} + \frac{\partial \tau_{zy}}{\partial z} + F_y = 0 \\ \frac{\partial \tau_{xz}}{\partial x} + \frac{\partial \tau_{yz}}{\partial y} + \frac{\partial \sigma_z}{\partial z} + F_z = 0 \end{cases} \quad (4.1)$$

$$\begin{cases} \tau_{xy} = \tau_{yx} \\ \tau_{yz} = \tau_{zy} \\ \tau_{xz} = \tau_{zx} \end{cases} \quad (4.2)$$

Where σ_i , with i varying between x , y and z , represents the normal stress relative to the plane with normal i , while τ_{ij} , with i and j varying between x , y and z , represents the tangential stress along the j axis relative to the plane with normal i , and, finally, with F_x , F_y and F_z being the force components along the x , y and z axes.

The Compatibility equations (Eq. 4.3), enforce the structure to deform in such a way that no breakages nor interpenetrations occur. This condition translates into equations relating the deformations of any elementary volume composing the structure to the respective displacements:

$$\begin{cases} \varepsilon_x = \frac{\partial u}{\partial x} \\ \varepsilon_y = \frac{\partial v}{\partial y} \\ \varepsilon_z = \frac{\partial w}{\partial z} \\ \gamma_{xy} = \frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \\ \gamma_{yz} = \frac{\partial v}{\partial z} + \frac{\partial w}{\partial y} \\ \gamma_{zx} = \frac{\partial w}{\partial x} + \frac{\partial u}{\partial z} \end{cases} \quad (4.3)$$

Where ε_i with i varying between x , y and z , represents the normal deformation relative to the plane with normal i , γ_{ij} , with i and j varying between x , y and z , represents the tangential deformation in the plane normal to the axis orthogonal to both i and j , and, finally, u , v and w are the displacements along the x , y and z axis respectively.

The Constitutive equations (Eq. 4.4), express the physical characteristics of the material composing the structure in terms of relations between the deformations of any elementary volume composing the structure and the relevant stresses:

$$\left\{ \begin{array}{l} \varepsilon_x = \frac{1}{E} [\sigma_x - \nu(\sigma_y + \sigma_z)] + \alpha \Delta T + \varepsilon_{sw} + \varepsilon_{cr} \\ \varepsilon_y = \frac{1}{E} [\sigma_y - \nu(\sigma_x + \sigma_z)] + \alpha \Delta T + \varepsilon_{sw} + \varepsilon_{cr} \\ \varepsilon_z = \frac{1}{E} [\sigma_z - \nu(\sigma_x + \sigma_y)] + \alpha \Delta T + \varepsilon_{sw} + \varepsilon_{cr} \\ \gamma_{xy} = \frac{1}{G} \tau_{xy} \\ \gamma_{yz} = \frac{1}{G} \tau_{yz} \\ \gamma_{zx} = \frac{1}{G} \tau_{zx} \end{array} \right. \quad (4.4)$$

Where, considering the hypothesis of homogeneous, isotropic and linear elastic material, it is possible to express the elastic properties of the body with E , ν and G , called engineering constants, which can be determined experimentally, representing Young's modulus, Poisson's coefficient and tangential elasticity modulus respectively, while α is the coefficient of thermal expansion and finally the ΔT , is the difference between the operating temperature and the one in which the body is considered not to be deformed due to the effect of temperature. The terms ε_{sw} and ε_{cr} , on the other hand, concern the deformations induced in the body by the two phenomena of swelling and creep.

The resulting system of equation (Eqs. 4.1 to 4.4) is a system of linearly independent algebraic-differential equation at partial derivatives, for which a closed analytical solution can be found only in problems characterized by extremely simple geometries, loads and BCs. Hence, analogously to the thermofluid-dynamic and thermal problems, also the mechanical problem is generally targeted through numerical methods. More specifically, the mechanical problem – as the thermal problem within the COB segment – has been solved through a FEM based calculation.

The Finite Element Method is one of the most widely used numerical methods in the engineering field as it enables to solve the thermomechanical problem even for bodies that present a complex spatial domain and thermophysical properties that are strongly dependent on variables such as position, temperature or time [68]. To address through FEM the thermomechanics of the HCPB monoblock COB segment, and, subsequently, of a pin thereby located, it has been necessary to create the relevant 3D CADs, which has been done by means of the Ansys SpaceClaim 3D CAD modelling software [60]. The availability of the CAD allowed to proceed applying the FEM, which envisages that the examined body is discretized into many finite elements, the vertices of which are referred to as nodes. This discretization process has been performed by means of the Ansys Mesh Tool [61]. Subsequently, it is necessary to properly define the loads and BCs of the problem, that can thus be solved numerically: these phases have been carried out through Ansys Mechanical [20]. In more detail, adopting the FEM, the governing equations are solved within the

elements considering appropriate conditions at the interfaces, and the unknowns are computed in the nodes. The values of the unknowns at locations that are within the elements but not at the nodes are obtained by interpolating the nodal values using shape functions. The analytical form of the shape function depends on the type of element that is employed and typically corresponds to a first or second order interpolation scheme. As it can be deduced, a finer mesh will provide solutions closer to the real case than a coarser discretization, but the computational burden will be greater: performing the trade-off between accuracy and computational cost is up to the analyst. The mesh adopted in the presented analyses has been validated through the evaluations outlined in section 4.5.

Another key methodological aspect in the thermomechanical investigation on an HCPB monoblock pin located in the COB segment, consists in a particular way in which FEM analyses have been performed, namely a multiscale approach referred to as submodeling.

The submodeling is a technique to study a local part of a wider model with a refined mesh, based on the mapping of the solution from an initial wider model (more coarsely discretized) on the boundary regions (i.e. the cut faces or edges) of the submodel. Hence, the submodel represents a cut through the larger complete model (Figure 4.1), and is a region of particular interest, where a more detailed analysis is performed imposing BCs (in terms of displacement, stress or temperature mapped from the global model) reproducing the actions exerted by the rest of the structure on the submodel.

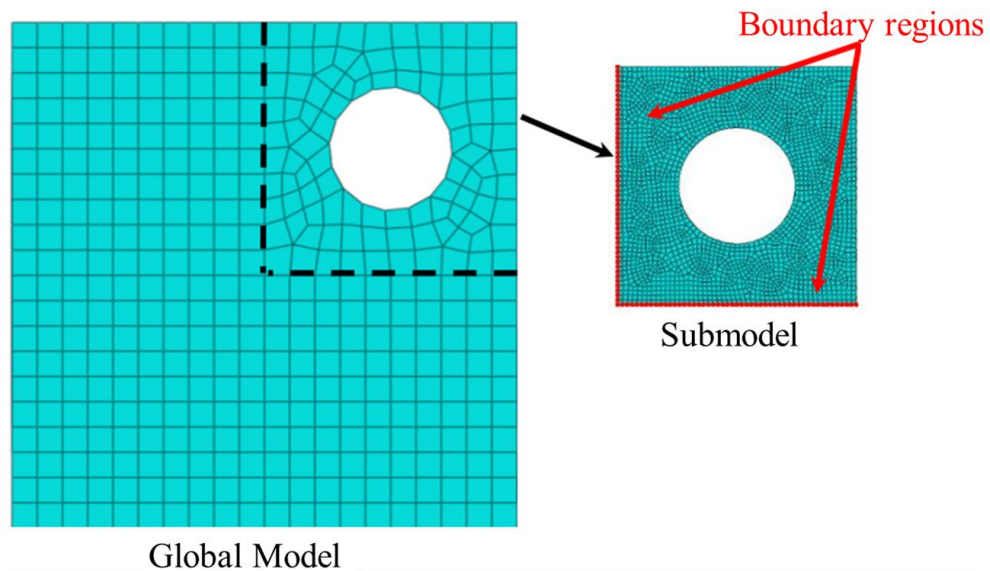


Figure 4.1: Global model and submodel mesh.

In the case of the thermomechanical study of an HCPB monoblock BB pin, the BB concept foresees a blanket structure made of clusters of pins (as it was shown in Figure 1.14) that are in contact with each other. Hence, the structural performance of a single pin cannot be evaluated without an estimation of the actions exerted on it by the surrounding pins.

Additionally, the mutual actions exchanged by the pins depend on the global behaviour of the relevant blanket segment (the COB segment in the analyses detailed hereby). However, studying the behaviour of a whole HCPB monoblock blanket segment with a discretization fine enough to realistically predict the stresses occurring within every single pin has a prohibitively high computational cost.

For this reason, it has been decided to address the thermomechanical problem adopting the submodeling technique. In more detail, the overall behaviour of the HCPB monoblock COB segment has been assessed through a global model not featuring the pin internals, but, importantly, loaded with the thermal field corresponding to the specific pin layout considered (either the case 2 or case 5 geometry). From this global model, a single pin has been extracted as a submodel, and the pin internals (of the specific layout considered) have been added (Figure 4.2): this local geometry has been discretized with a finer mesh – adequate for capturing the structural behaviour of the pin internals, which are on a rather different scale than the COB segment – and has been analysed with the BCs (in terms of displacement field) directly obtained from the global model analysis (rather than arbitrarily imposed).

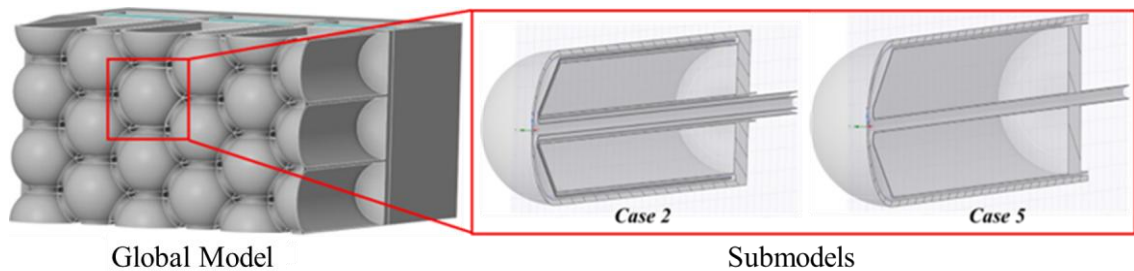


Figure 4.2: Global model and submodels for analysis on a pin of the HCPB monoblock COB segment (submodels shown in section).

As a result, the stress fields within both pin arrangements have been evaluated considering realistic boundary conditions arising from the COB segment behaviour. The knowledge of the stress field has allowed to perform the structural verifications according to the RCC-MRx structural design code [19], exploiting a procedure denoted as stress linearization.

In more detail, the design practice defined for nuclear components – necessary to allow licensing through a Nuclear Regulator – includes distinguishing load conditions depending on their expected occurrence according to a nuclear code [69]. For DEMO, on the basis of ASME [70] and IAEA [71] guidelines, four Categories (Cat) have been defined along with the respective frequencies of occurrence:

- Cat I includes *operational* loading conditions, i.e. conditions intentionally triggered by the plant operator.

- Cat II includes *expected* loading conditions, i.e. conditions that are expected to occur in the life of the plant up to about 100 times.
- Cat III includes *possible* loading conditions, i.e. conditions that are expected to occur less than about once during the plant life.
- Cat IV are *unlikely* loading conditions, i.e. conditions with an expected frequency of occurrence of less than once every 10,000 years.

Moreover, the design codes define criteria levels on the basis of the specific structural damage that is aimed to be prevented [70]:

- Level A and B: No damage requiring repair occurs. The plant shall be able to resume operation without special maintenance or test.
- Level C: Large (plastic and hence permanent) deformations permitted in areas of structural discontinuity. Shutdown for component inspection and repair may be required before proceeding operation.
- Level D: Gross general (plastic and hence permanent) deformations permitted including some loss of dimensional stability, e.g. local buckling. Component repair or replacement may be required.

Table 4.1 details the default association of loading conditions to damage criteria in DEMO.

Table 4.1: Association between loading conditions and damage criteria for DEMO [69].

Loading condition	Damage criterion
Cat I	Level A
Cat II	Level A
Cat III	Level C
Cat VI	Level D

The verifications carried out on an HCPB monoblock pin located in the COB segment have been done according to the Level A criteria of the RCC-MRx structural design code [19], relevant (according to Table 4.1) to *operational* and *expected* loading conditions.

The criteria specified by the design code are typically expressed in the form of inequalities of the type (Eq. 4.5):

$$\sigma < \sigma_{lim} \quad (4.5)$$

Where σ_{lim} represents the limit value allowable for σ for the component to operate fulfilling the considered damage criterion, and σ is a scalar that represents the tensional state

in the body. Given Eq. 4.5, performing the verification of the criterion can be translated into computing the so-called *utilization factor* as the ratio given by Eq. 4.6, and checking that it is less than unity, possibly with a certain margin to cope with uncertainties and approximations.

$$UF = \frac{\sigma}{\sigma_{lim}} \quad (4.6)$$

Given the complexity of the tensional state of the body – that is defined by a stress tensor that varies in space within the structure – the definition of a scalar σ representative of the severity of the stress state is a nontrivial task. To this scope, the procedure established by the most important standards relating to nuclear components (ASME [70], RCC-MRx [19], SDC-IC [72], etc) is based on two underlying principles:

- The risk associated with a tensional state depends on the causes that determine it. This consideration leads to the distinction between *primary* and *secondary* stresses.
- The risk associated with a tensional state cannot be determined through pointwise sampling of the stress tensor in the most critical points; rather, it is necessary to consider the spatial distribution of the stress tensor throughout the most critical thicknesses. This consideration leads to the definition of the *stress linearization* procedure.

As to the causes of the tensional state, the *primary* stresses (denoted as P) are the tensions that are required to satisfy equilibrium with external loads. As a consequence, primary stresses do not have upper bounds and can lead to failure in case of excessively severe external loading. On the other hand, *secondary* stresses (denoted as Q) are tensions that are not required for equilibrium, but arise due to the presence of constraints (e.g., geometric, compatibility, or BCs). These stresses are self-limiting and can relax through local yielding. Thermal stresses are an example of secondary stresses.

In order to determine the breakdown of the total stresses (P+Q) into primary stresses P, and secondary stresses Q, multiple analyses have been conducted on the HCPB monoblock (as it will be detailed in the following sections), considering primary and secondary loads separately.

As to the *stress linearization*, it is a procedure that is performed in the most critical regions (sections) of the component, where 1D “paths” have to be defined across the thickness of the body. This method can be applied to any stress tensor (be it P, Q, or P+Q) and consists

in breaking down the stress tensor obtained along the thickness into the sum of three components:

- mean value or membrane stress.
- Triangular component or bending stress.
- Nonlinear component or peak stress.

Where, according to SDC-IC [72], considering a path of length h along the X_3 axis – defined with its origin at the midpoint of the path – the components of the membrane stress tensor are evaluated by Eq. 4.7, those of the bending stress through Eq. 4.8, and those of the nonlinear stress tensor by Eq. 4.9.

$$(\sigma_{ij})_m = (1/h) \int_{-h/2}^{+h/2} \sigma_{ij} dx_3 \quad (4.7)$$

$$(\sigma_{ij})_b = (12x_3/h^3) \int_{-h/2}^{+h/2} \sigma_{ij} x_3 dx_3 \quad (4.8)$$

$$(\sigma_{ij})_{nl} = \sigma_{ij} - [(\sigma_{ij})_m + (\sigma_{ij})_b] \quad (4.9)$$

A graphical representation of the stress linearization procedure is provided in Figure 4.3.

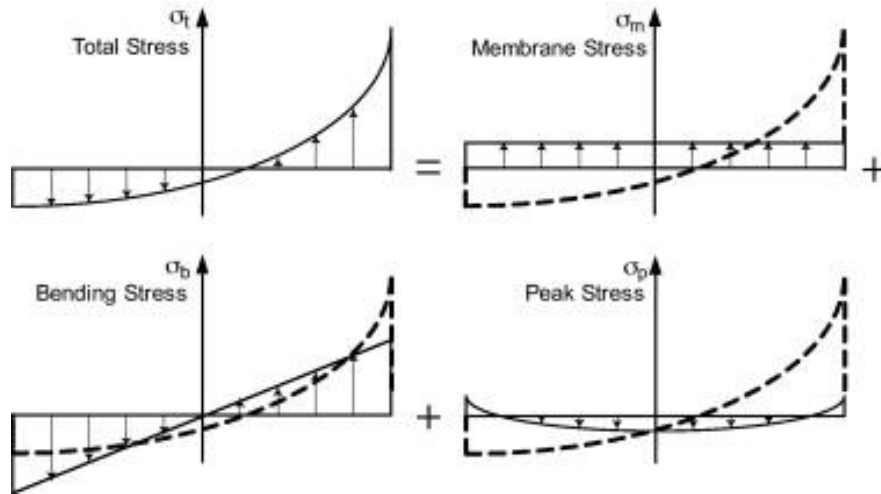


Figure 4.3: Stress linearization [73].

Hence, through stress linearization, the primary, the secondary, and the total stress tensors can be divided, along a path, into their respective membrane, bending and peak components. From the so-obtained tensors, scalar quantities can be derived by computing the second-order invariant. As to the bending tensor, the second-order invariant is often evaluated at the endpoints of the path, where the tensions have the highest absolute values. Typically, the

Von Mises stress (Eq. 4.10) is adopted as the second-order invariant, as it is linked to the deformation energy:

$$\sigma_{VM} = \sqrt{\frac{1}{2} \left[(\sigma_x - \sigma_y)^2 + (\sigma_x - \sigma_z)^2 + (\sigma_y - \sigma_z)^2 + 6(\tau_{xy} + \tau_{xz} + \tau_{yz})^2 \right]} \quad (4.10)$$

Hence, coming back to Eq. 4.6, the structural design codes typically define σ as the Von Mises stress (or a linear combination of Von Mises stresses) of either a stress tensor or a linear combination of stress tensors obtained through the stress linearization of either the primary, or the secondary, or the total stress tensor. To provide an example, the criteria may involve the computation of the Von Mises of the membrane component of the secondary stress tensor Q_m , or may consider the bending component of the primary stress tensor P_b . The exact definition of σ depends on the specific criterion (i.e. the type of damage to be prevented and the damage level considered). In this way, the criterion can effectively take into account – by means of σ – the cause of the stress (primary or secondary) and its spatial distribution across the thickness of critical regions (membrane, bending, peak), while also considering – by means of σ_{lim} – the admitted damage level, dependent on the likelihood of the examined scenario.

In the structural analysis conducted on a pin of the HCPB monoblock COB segment, the stress linearization procedure has been carried out within the pin domains, and the level A criteria prescribed by the RCC-MRx [19] structural design code have been considered against three types of damage, namely Immediate Excessive Deformation (IED), Immediate Plastic Instability (IPI), and Immediate Plastic Flow Localization (IPFL). These are, in fact, the most commonly adopted rules in DEMO BB design. The relevant utilization factors are shown in Eq. 4.11, 4.12, and 4.13, where the overline is used to indicate that stress intensity (i.e. the Von Mises stress) is computed.

$$UF_{IED} = \frac{\overline{P_m}}{S_m} \quad (4.11)$$

$$UF_{IPI} = \frac{\overline{P_m + P_b}}{K_{eff} S_m} \quad (4.12)$$

$$UF_{IPFL} = \frac{\overline{P_m + Q_m}}{S_{em}} \quad (4.13)$$

Where the stress limits S_m and S_{em} are detailed in the code [19], and are evaluated at the path average temperature and considering a radiation damage of 20 displacements per atom (dpa), and K_{eff} is a geometric factor assumed to be equal to 1.5 for the pin geometry [19]. It is to be pointed out that 20 dpa is a rather high level of damage, as it corresponds to the estimated radiation damage of the First Wall at the end of the DEMO BB duty life.

The description of the stress linearization procedure and of the rationale behind the adopted structural verification criteria closes the digression on the methodology adopted for the exploratory thermomechanical study, which is detailed in the following subsections.

4.3 Global FEM analysis

As described in section 4.2, the thermomechanical problem of an HCPB monoblock pin located in the COB segment has been targeted through the submodeling technique. Hence, the first study (detailed in the present section) focused on the assessment of the global behaviour of the HCPB monoblock COB segment, allowing to gather precious information to be exploited in the subsequent phase, namely the thermomechanical study of a single pin (submodel), detailed in section 4.4. It is hereby recalled that, following the thermofluid dynamic analysis campaign (Chapter 3), it has been decided to analyse two different kinds of pin arrangement, exhibiting major geometrical and thermal differences: one geometry characterised by a double coolant flow pass and one envisaging a single flow pass. In more detail, the case 2 and the case 5 layout have been selected, by virtue of being the two geometries exhibiting the lowest Eurofer temperatures with an allowable coolant pressure drop. Moreover, the structural response has been assessed considering, for both the pin geometries, the respective thermal fields relevant to the reference working conditions (HF_0 of 0.3 MW/m^2 , coolant ΔT of $220 \text{ }^\circ\text{C}$), with roughness on machinable surfaces of $200 \text{ }\mu\text{m}$. This choice is aimed at performing the structural study considering the conditions that guarantee the lowest Eurofer temperatures, and, therefore, supposedly most favourable. In this way it is possible to draw conclusion on the concept's viability from the thermomechanical standpoint.

Within the present subsection, firstly (section 4.3.1) the setup of the global analysis is described, and subsequently the results (4.3.2) are outlined.

4.3.1 Global FEM analysis setup

Starting from the provided geometric layout of half slice of the HCPB monoblock BB [74], a more extended geometric model has been created (Figure 4.4). Exploiting the COB segment intrinsic toroidal symmetry, a triplet of poloidally adjacent half slices has been created by properly duplicating upward and downward the provided geometry. In this way,

the CAD model of a considerable portion of the equatorial region of the HCPB monoblock COB has been obtained. Hence, imposing boundary conditions on the top and on the bottom of the triplet of slices would allow the central one to be far enough for the assumed mechanical restraints, so that the results thereby computed can be retained reliable. Therefore, the 3D geometric model shown in Figure 4.4 has been generated and adopted for the first part of the campaign of structural analysis.

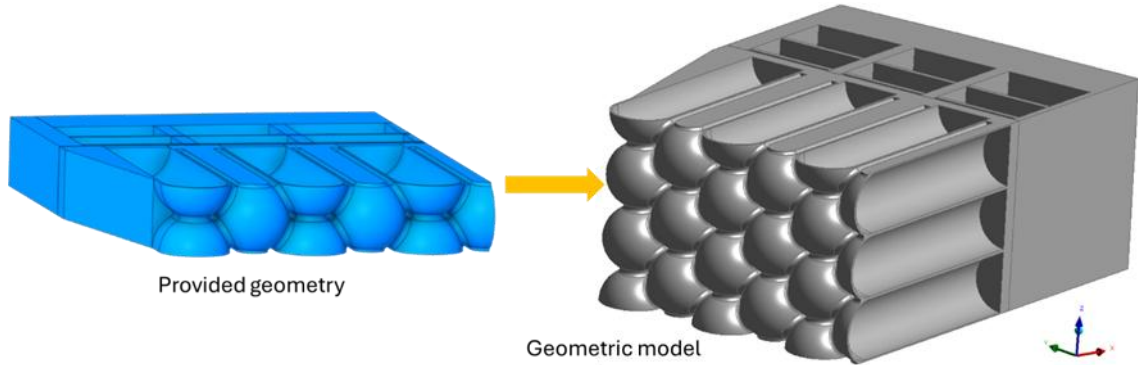


Figure 4.4: Global model analysis - the 3D geometric model of the equatorial region of the COB segment [47].

It has to be noted that the so generated 3D geometric model includes the pins external envelope (consisting in the pin cap, shell and plug) plus the proper portion of the manifolds region, of the side wall and of the Back-Supporting Structure (BSS), as depicted in Figure 4.5. Moreover, the connecting regions to form the so-called honeycomb configuration have been included too. Therefore, as anticipated in section 4.2, the global model layout is independent, from the geometric point of view, from the pin internal architecture, namely from the specific design option (case 2 and case 5) considered.

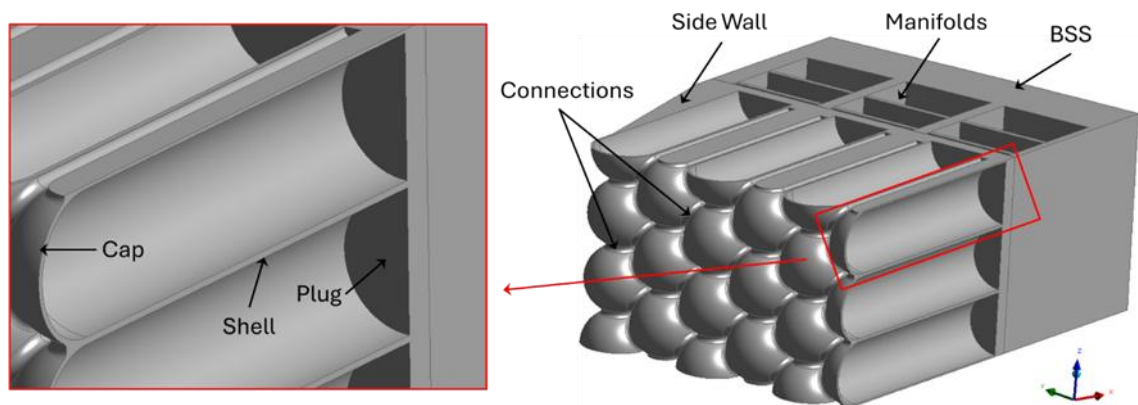


Figure 4.5: Global model analysis - details of the 3D geometric model [47].

Once obtained the reference geometric configuration, proper 3D FEM models have been set up in order to assess the structural behaviour of the equatorial region of the HCPB monoblock COB segment conceived according to case 2 and case 5 architectures. Indeed,

although the difference in pin internals does not affect the global model geometry, it affects the thermal field, which is relevant in determining the global structural response.

As a first step, a mesh (Figure 4.6) composed by $2.75 \cdot 10^6$ nodes connected in $1.28 \cdot 10^6$ hexahedral and tetrahedral quadratic elements has been generated. Eurofer has been considered as structural material and its temperature-dependent thermophysical properties, drawn from [75], have been implemented in Ansys Mechanical [20].

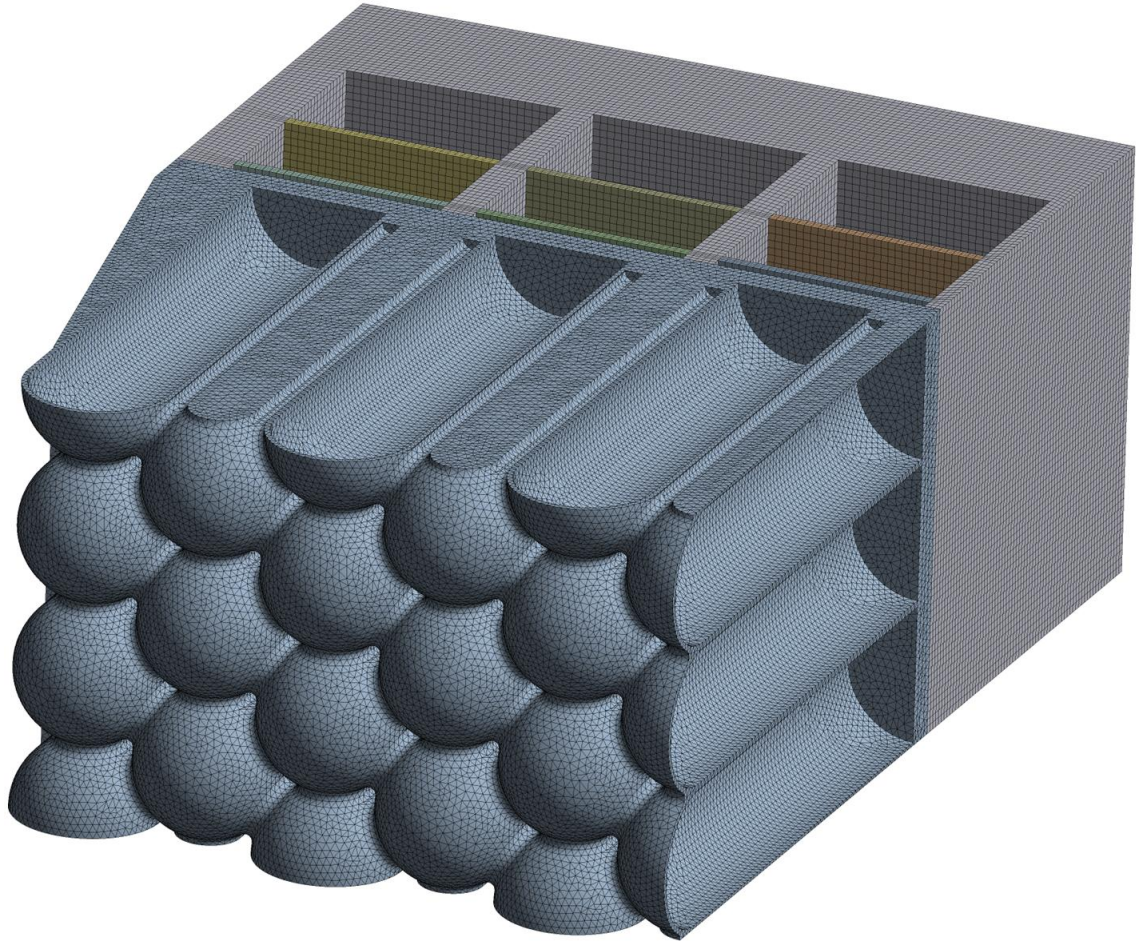


Figure 4.6: Discretization adopted for global model analysis.

Once carried out the spatial discretization grid, global thermal analyses have been setup, the scope being to obtain 3D thermal fields to be imported into the global structural calculations. In more detail, within the global model, the temperatures occurring in the pins have been determined through the thermofluid-dynamic analyses detailed in Chapter 3 (Figure 4.7), therefore the relevant 2D temperature spatial distributions have been properly mapped into the 3D FEM models and constituted an *imported load* of the analysis setup. In this regard, it is hereby recalled that the adopted thermal fields are relevant to the pin reference working conditions (HF_0 of 0.3 MW/m^2 , coolant ΔT of $220 \text{ }^\circ\text{C}$), with roughness on machinable surfaces of $200 \text{ }\mu\text{m}$.

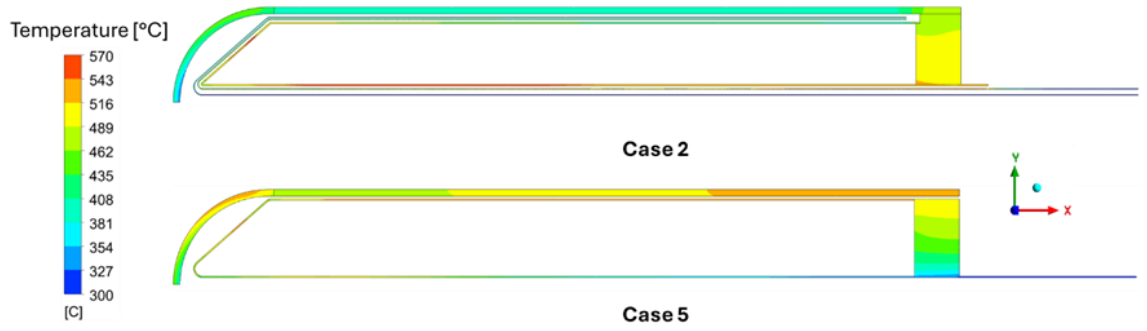


Figure 4.7: 2D thermal fields adopted for the thermomechanical assessment [47].

In more detail, the thermal load has been imported exploiting the axis-symmetric nature of the pin geometry: the calculated 2D thermal fields have been revolved and applied to all the pins included in the 3D FEM model. In this phase, only the temperatures calculated for the pin envelope (cap, shell and plug) have been mapped, as the global model does not envisage the pin internals. To provide an example, Figure 4.8 illustrates the mapping of the 2D thermal field of the case 5 pin onto the envelope of one pin of the 3D global model.

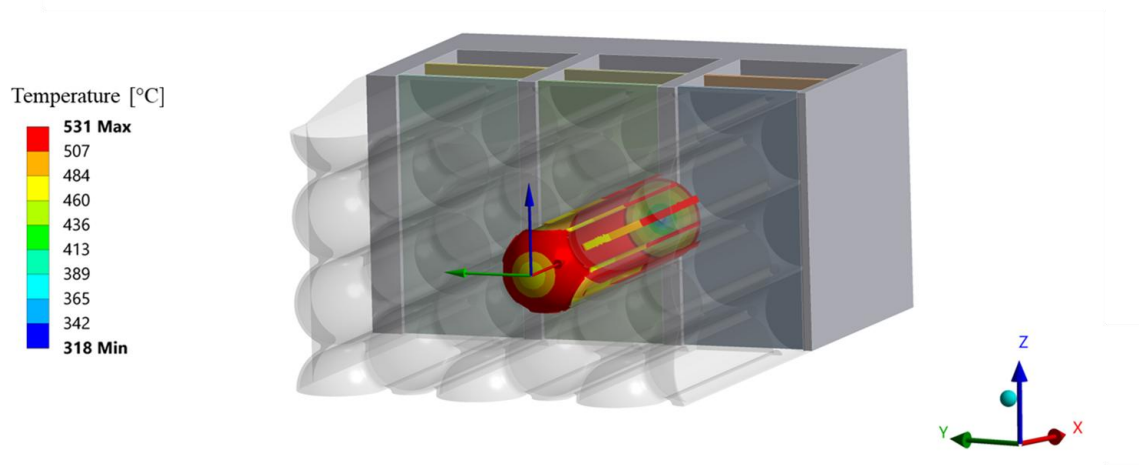


Figure 4.8: 2D thermal field of one pin (case 5 layout) mapped onto the global model as an imported load for the thermal analysis.

As it can be deduced, setting up the imported load for every pin required to perform many repetitive operations multiple times, resulting in a time consuming and error prone procedure. For this reason, exploiting the Python [76] scripting capabilities implemented in Ansys Mechanical [20], the procedure has been partially automated by the script shown in Figure 4.9.

To complete the setup of the global thermal analyses, coherently with the assumptions made in the thermofluid-dynamic optimization study, a temperature of 520 °C has been imposed on all the nodes lying in the helium-wetted surfaces of the outlet manifold, as well as a temperature value of 300 °C has been set to the nodes belonging to the coolant-wetted

surfaces of the inlet manifold. Then, the symmetry faces have been considered adiabatic, as well as the side wall and BSS external surfaces.

```

1  mdl = ExtAPI.DataModel.Project.Model
2  geom = mdl.Geometry
3  mat = mdl.Materials
4  cosys = mdl.CoordinateSystems
5  conn = mdl.Connections
6  msh = mdl.Mesh
7  ns = mdl.NamedSelections
8  anl = mdl.Analyses
9  gobi = DataModel.GetObjectById
10 gobn = DataModel.GetObjectsByName
11 gobt = DataModel.GetObjectsByType
12
13
14  for ii in cosys.Children:
15      if ii.Name.Contains("3D")==False:
16          CS 2 Duplicate=gobn("2D 3D B0")[0]
17          NewCS=CS 2 Duplicate.Duplicate()
18          NewCS.Name="2D_3D_"+ii.Name
19
20          #region Details View Action
21          NewCS.OriginX = Quantity(str(ii.OriginX))
22          NewCS.OriginY = Quantity(str(ii.OriginY))
23          NewCS.OriginZ = Quantity(str(ii.OriginZ))
24
25          a=gobn("IT_B0")[0]
26          NEW_IT=a.Duplicate()
27          NEW_IT.Name = "IT_"+ii.Name
28          NEW_IT.RigidBodyTransformationType=RigidBodyTransformationType.
CoordinateSystem
29          NEW_IT.RigidTransformSourceCoordinateSystem=gobn("Global Coordinate System")[0]
30          ]
31      #endregion

```

Figure 4.9: Ansys Python script that partially automates the mapping of the 2D pin thermal fields to the 3D global model.

The description of the setup of the global thermal analysis is completed, and the relevant results are detailed in section 4.3.2. The remaining part of the present section illustrates the setup of the global structural analysis.

Mechanical loads and boundary conditions have been applied to assess the structural behaviour of the equatorial region of the HCPB monoblock COB segment. Moreover, the 3D temperature distribution resulting from the global thermal analysis constitutes the thermal load, and a no stress temperature of 22 °C has been assumed to evaluate the thermally induced stress.

Regarding pressure loads, two different scenarios have been investigated, namely the steady-state nominal and design loading conditions. In the first, the nominal pressure of helium of 8.0 MPa, is assumed for the pins' and manifolds' surfaces wetted by coolant and purge gas. In the second scenario the design pressure, equal to 9.2 MPa (Table 2.2), is conservatively considered.

Regarding mechanical restraints, a combination of BCs has been imposed in order to simulate the poloidal continuity of the modelled region as well as the action of the BB attachment system. In particular, a vertical (i.e. along Z direction) symmetry condition has been imposed to the nodes laying onto the model's lower surface, whereas a Generalised

Plane Strain (GPS) condition has been imposed onto the model's upper face (Figure 4.10). The latter condition forces the nodes initially laying onto the model's upper face to remain onto the same plane, which can translate along the Z direction and tilt around the Y and X axes. Moreover, a Y symmetry condition has been imposed onto the toroidal plane of symmetry, so as to simulate the toroidal continuity of the modelled equatorial region. Lastly, the radial displacement (U_x) has been prevented to the nodes lying onto the red line depicted in Figure 4.10 with the scope to simulate the radial action of the blanket attachment system, ensuring the numerical convergence of the structural analysis.

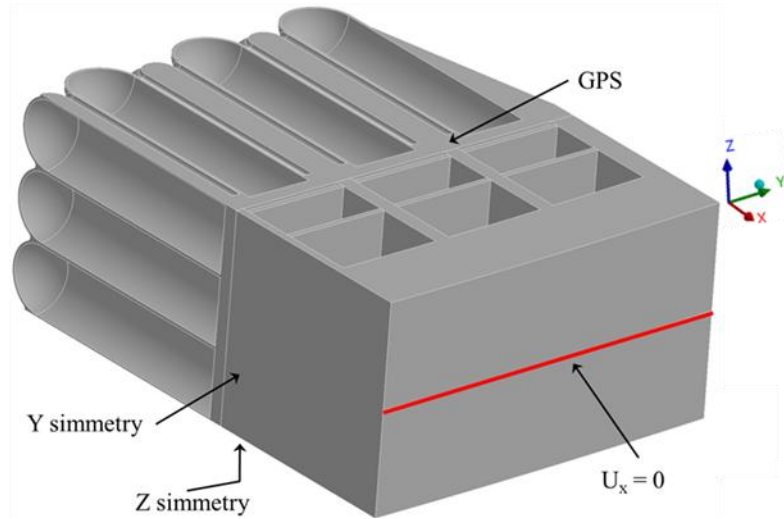


Figure 4.10: Global model analysis – mechanical restraints [47].

Finally, it is hereby recalled that in view of the verification against the level A criteria prescribed by the RCC-MRx [17] (Eq. 4.11, 4.12 and 4.13), it is necessary to know how the total stress tensor ($P+Q$) breakdowns into the primary P and the secondary stress Q . Therefore, the above-mentioned loads and BCs have been composed to carry out, for each pin layout, the following analyses:

- two analyses where all the loads relevant to the structural analysis are applied, aimed at determining the total stress ($P+Q$): one analysis performed imposing the nominal pressure, and one imposing the design pressure;
- one analysis performed solely imposing the thermal load and the mechanical restraints (hence, no pressure load), to assess the secondary stress tensor Q .

Ansys Mechanical [61] allows to define a *solution combination* as an arbitrary linear combination of any computed solution, therefore, once assessed the total and of the secondary stress tensors, it is possible to determine the primary stress tensor as:

$$P = (P + Q) - Q \quad (4.14)$$

Where Eq. 4.14 is to be evaluated both for the nominal and the design pressure loads.

4.3.2 Global FEM analysis results

Once assigned the proper loads and boundary conditions, thermal and structural steady-state analyses have been run to assess the overall behaviour of the equatorial region of the HCPB monoblock COB segment.

As to the global thermal analysis, although – as described in the previous section – the temperature in most of the calculation domain has been imposed as an imported load, thermal analyses have been necessary to calculate the thermal field in the side wall, manifolds and BSS bodies as well as within the pins connecting regions (i.e. the afore mentioned honeycomb structure). The results obtained are shown in Figure 4.11. It can be observed that, on average, case 5 presents higher temperatures, which may appear contradictory when compared to the thermal fields in Figure 4.7, where only case 2 layout presents temperatures higher than 550 °C in Eurofer domains. However, this apparent discrepancy can be explained by recalling that the 3D global models do not include the pins' internals, which is where the highest Eurofer temperatures occur in the case 2 layout.

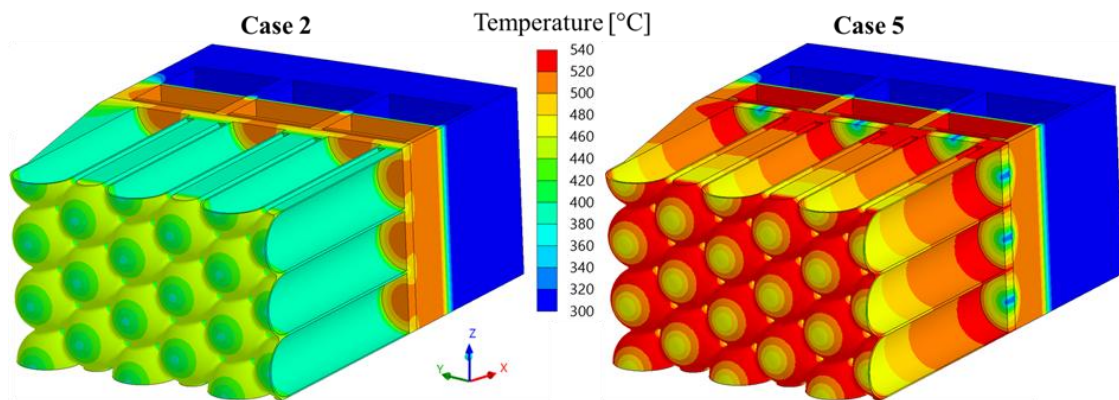


Figure 4.11: Global model analysis – 3D thermal fields [47]

As to the structural analyses, this section illustrates the results obtained considering the total stresses, being the utility of the other cases limited to the structural design criteria verification (illustrated in section 4.4.2).

Herein, the structural response of the global model for case 2 and case 5 geometric configurations is shown in terms of equivalent Von Mises stress field and displacement field. In particular, as to the nominal pressure load case, the Von Mises stress field and the total displacement 3D spatial distribution are depicted in Figure 4.12 and Figure 4.13 for case 2, whereas the analogous fields calculated for case 5 geometry are shown in Figure 4.14 and Figure 4.15, respectively.

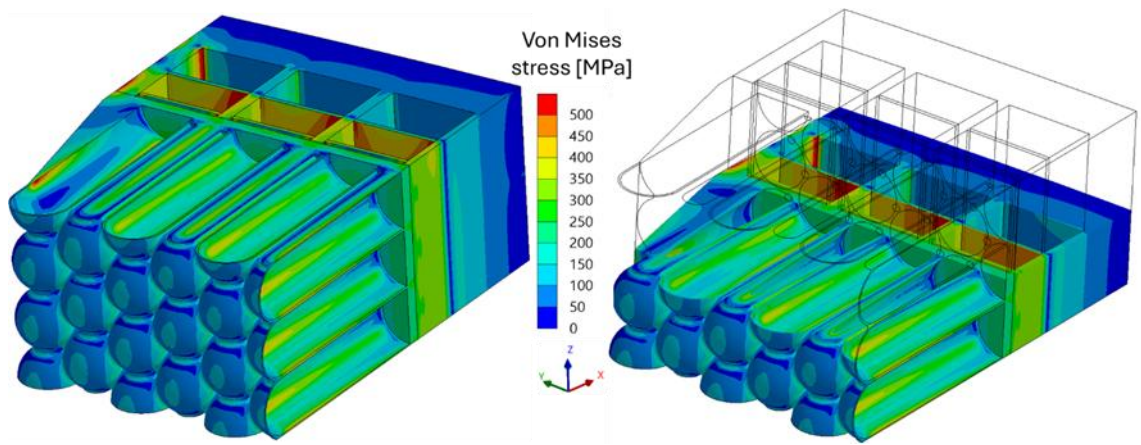


Figure 4.12: Global model - Case 2 - Nominal pressure - Von Mises equivalent stress [47].

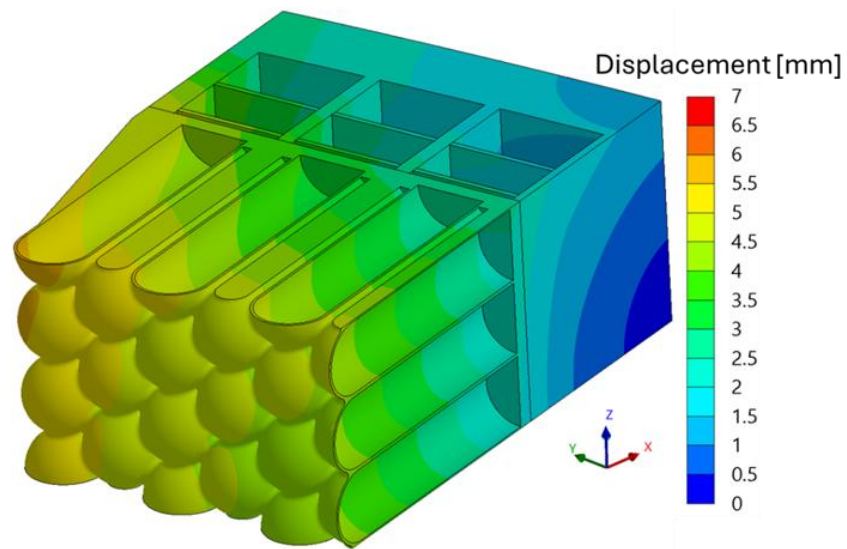


Figure 4.13: Global model - Case 2 - Nominal pressure - Total displacement [47].

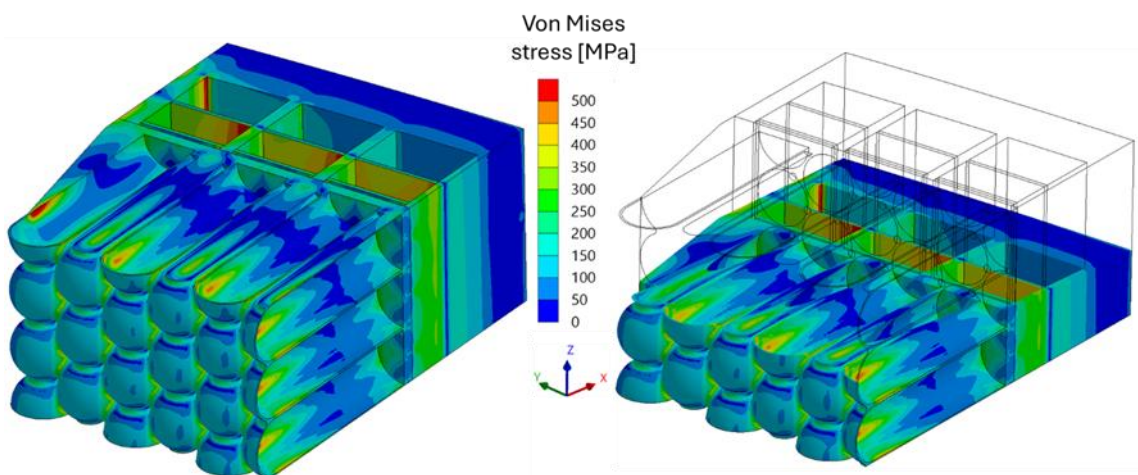


Figure 4.14: Global model - Case 5 - Nominal pressure - Von Mises equivalent stress [47].

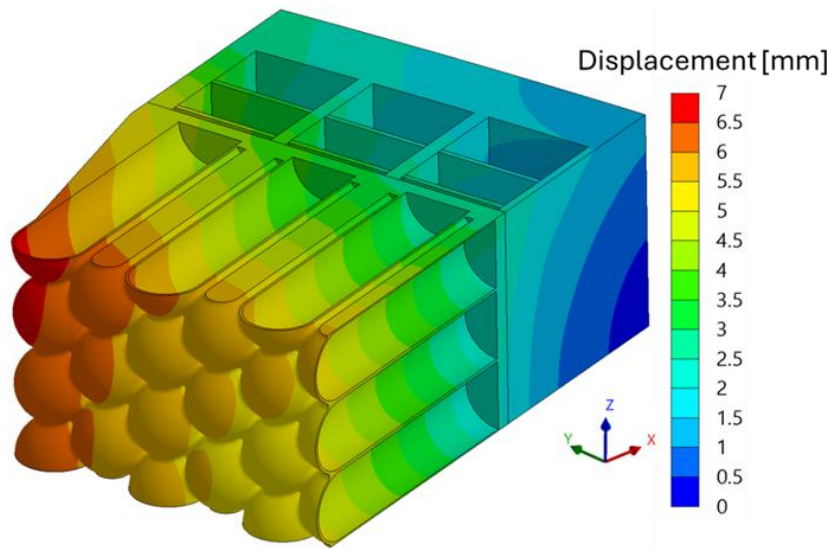


Figure 4.15: Global model - Case 5 - Nominal pressure - Total displacement [47].

It can be observed that reasonable values for both Von Mises stress and total displacement are predicted. Highest values are calculated when considering the case 5 pin layout, indicating that the pin envelope is more stressed in this pin architecture due to the higher temperatures. Moreover, for both the pin geometries, no particular stress concentration is observed in the pins connection structures, meaning that the honeycomb configuration is robust enough to keep the pins in their mutual positions without suffering excessively. Indeed, as further confirmation, a quite uniform displacement distribution is predicted among the pins. Some particularly stressed regions emerged in correspondence of sharp corners or edges as well as in the areas where boundary conditions have been defined: this kind of regions are beyond the validity of the FEM model that has been setup, and also beyond the scopes of the thermomechanical study of the HCPB monoblock pin geometry. Therefore, they have not been deemed worth of further attention. Moreover, high stress is also predicted within the manifold region. However, the manifold architecture has not yet been optimized for the HCPB monoblock BB, and the assessed manifold region represents a kind of dummy body used to connect the pins and thus to allow imposing some realistic BCs in the relevant submodel analysis. Consequently, the high stresses calculated in the manifold region are not to be considered as outcomes characterising the HCPB monoblock BB thermomechanical performance, but rather as an occurrence in the procedure to obtain valid BCs to be imposed in the study of a single HCPB monoblock pin. Nevertheless, the behaviour exhibited by the dummy manifolds assessed in the global analyses can be considered as a starting point when designing a proper manifold region for the HCPB monoblock BB. In this sense, the results herein presented can pave the way for future and more detailed conceptual design analysis of the manifolds region.

Analogously, the results obtained for the design pressure load case are reported in Figure 4.16 and Figure 4.17 for the case 2 pin layout as well as in Figure 4.18 and Figure 4.19 for the case 5 geometry.

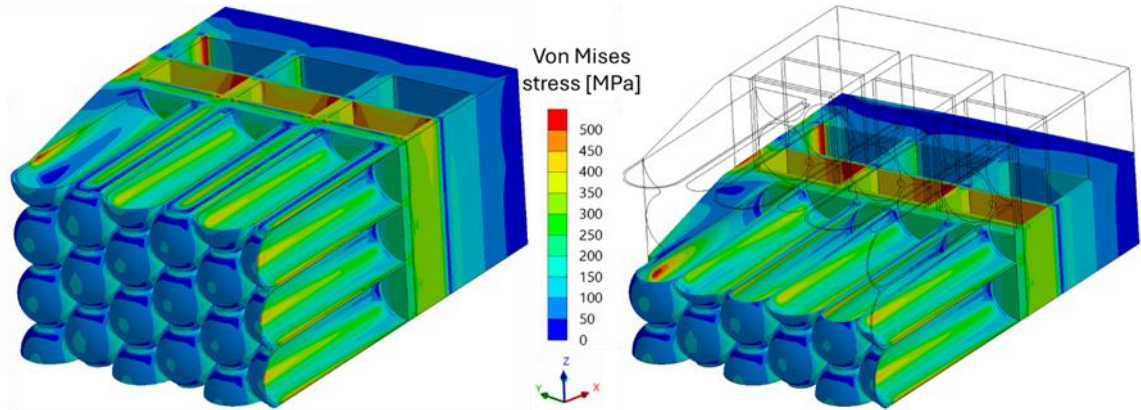


Figure 4.16: Global model - Case 2 - Design pressure - Von Mises equivalent stress [47].

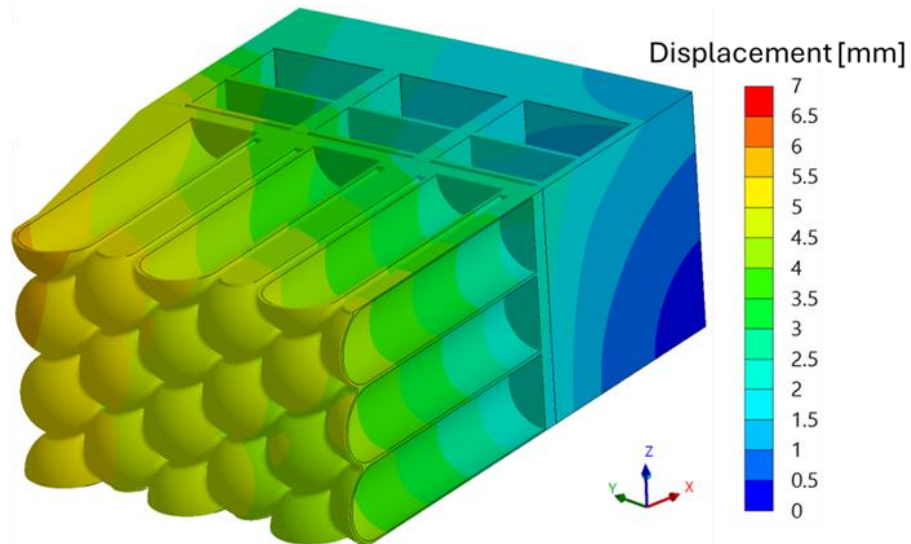


Figure 4.17: Global model - Case 2 - Design pressure - Total displacement [47].

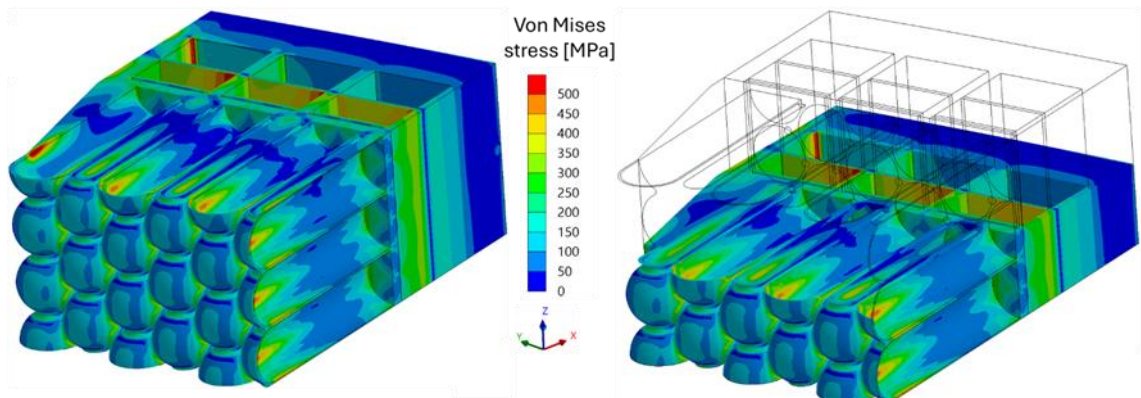


Figure 4.18: Global model - Case 5 - Design pressure - Von Mises equivalent stress [47].

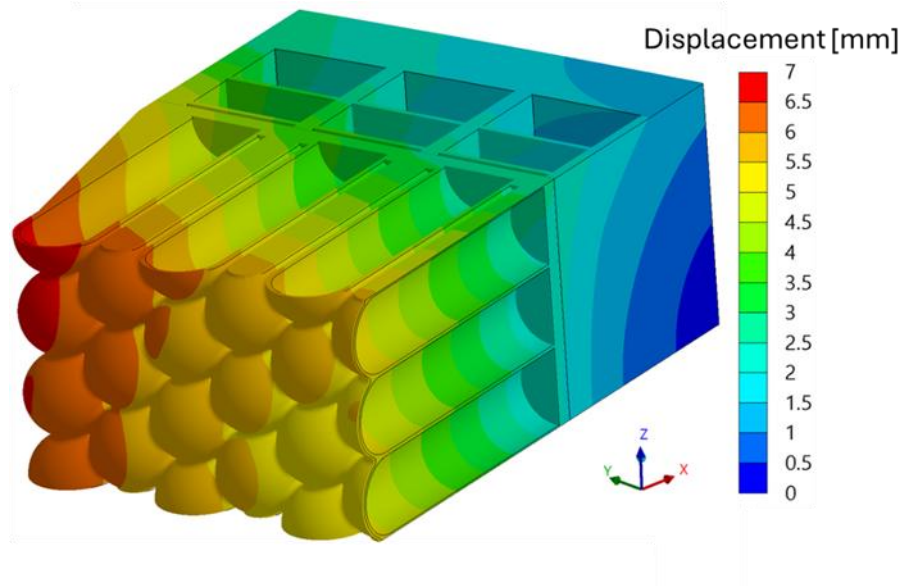


Figure 4.19: Global model - Case 5 - Design pressure - Total displacement [47].

Qualitatively, the same trends emerge when considering the nominal and design pressure scenarios.

4.4 Submodel analysis

As described in section 4.2, the thermomechanical problem of an HCPB monoblock pin located in the COB segment has been targeted through the submodeling technique. Hence, the first study (detailed in section 4.3) focused on the assessment of the global behaviour of the HCPB monoblock COB segment, with the scope to determine realistic BCs to be imposed at the boundaries of the submodel, namely a single pin located in the COB segment.

Hence, the submodel analysis – detailed in the present section – has allowed to assess the thermomechanical behaviour of the HCPB monoblock pin (designed according to the case 2 and the case 5 layouts), that is the final objective of the exploratory thermomechanical study. In the following, subsection 4.4.1 details the setup of the submodel analysis, and subsection 4.4.2 illustrates the relevant results.

4.4.1 Submodel analysis setup

The 3D CAD of the HCPB monoblock pin has been created, both for the case 2 and the case 5 layout, starting from the relevant 2D designs that had been created from scratch in Ansys SpaceClaim [60] with the scope to perform the thermofluid-dynamic analysis campaign (Chapter 3). In more detail, the respective 2D geometries have been revolved around the pin's axis to obtain the 3D geometric model.

As previously shown in Figure 4.2 the external parts of the so-obtain 3D pin CADs (namely the Eurofer cap, shell, and plug) can be perfectly superimposed to any of the pin envelopes of the global model. It has been decided to suppose the pins to be located at the

position indicated in the red box of the global model shown in Figure 4.2, as that location is far from the parts where the boundary conditions have been imposed in the global model analysis (section 4.3), and hence adequate to represent a generic pin located in the HCPB monoblock COB segment.

Once generated the pin geometric layouts, proper 3D FEM models have been set up in order to assess in detail their structural response under the selected loading conditions. As to case 2, a discretization (Figure 4.20) composed of $4.75 \cdot 10^6$ nodes connected in $2.01 \cdot 10^6$ quadratic tetrahedral elements has been performed, whereas regarding case 5 a mesh (Figure 4.21) consisting of $5.43 \cdot 10^6$ nodes connected in $3.30 \cdot 10^6$ quadratic tetrahedral elements has been realised. Eurofer steel has been considered also for the pin internal components.

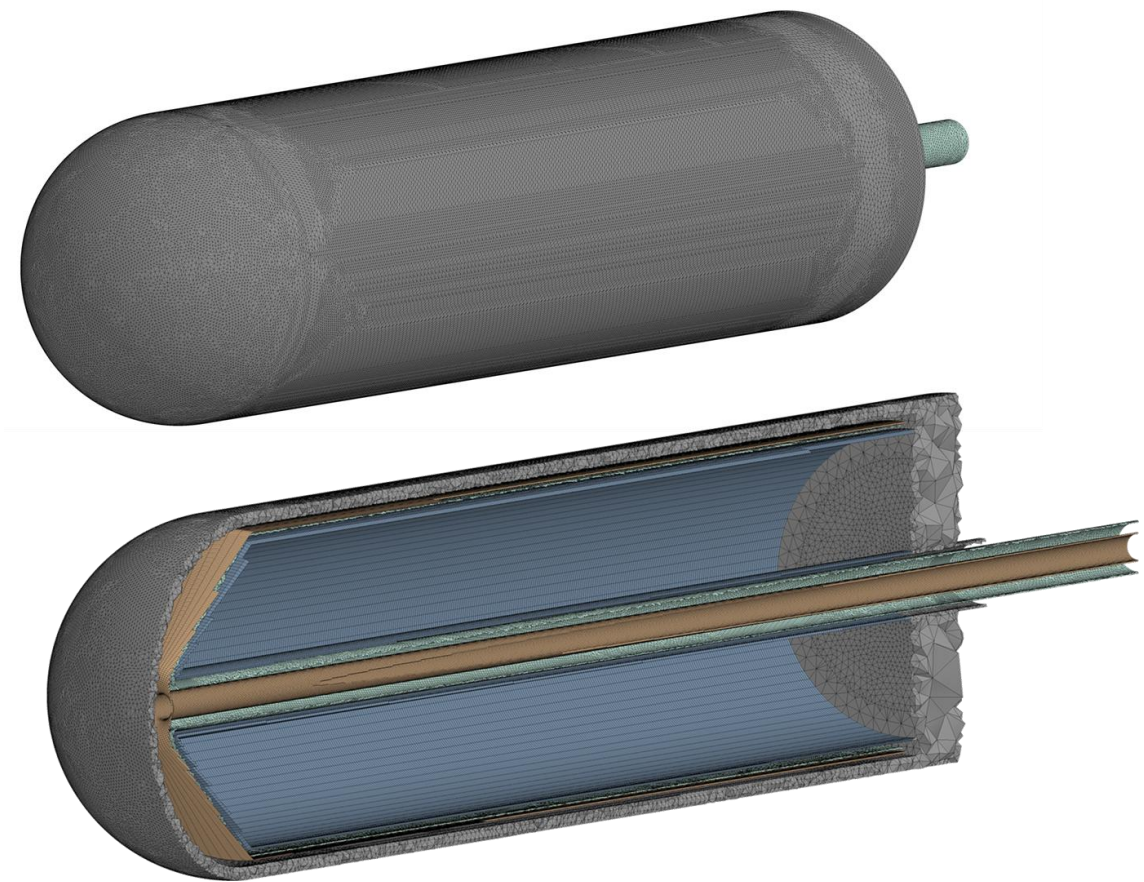


Figure 4.20: Discretization adopted for case 2 submodel analysis: full geometry (top) and section view (bottom).

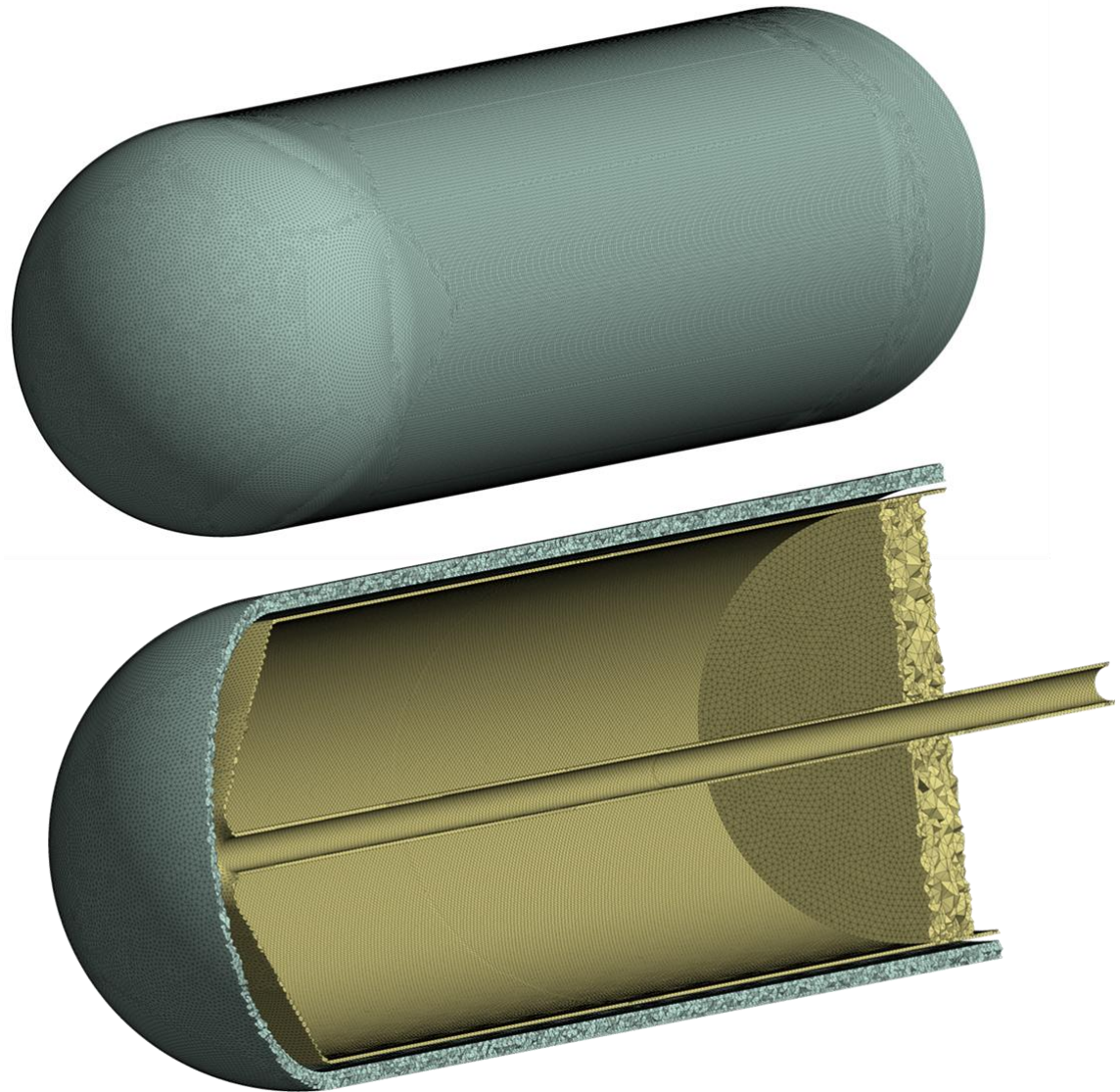


Figure 4.21: Discretization adopted for case 5 submodel analysis: full geometry (top) and section view (bottom).

Mechanical loads and boundary conditions have been applied to assess the thermomechanical behaviour of a pin located in the HCPB monoblock COB segment.

Regarding the 3D thermal field – imposed to ensure the consideration of the thermal induced stress, assuming a no-stress temperature of 22 °C – a combined mapping approach has been followed for the different pin components. Indeed, as to the pin external envelope, the thermal field has been directly mapped from the global model thermal analysis. Instead, as to the pin internals, the 2D temperature spatial distributions (Figure 4.7) assessed in the thermofluid-dynamic analysis campaign (Chapter 3) have been properly imported into the 3D FEM models exploiting the axisymmetric nature of the pin geometry (revolving the 2D thermal field around the pin's axis, in analogy to what has been done in the global model analysis and has been shown in Figure 4.8). Hence, the resulting 3D thermal fields applied

for structural analysis are shown in Figure 4.22 and Figure 4.23, where longitudinal sections of the pins are shown for case 2 and case 5 respectively.

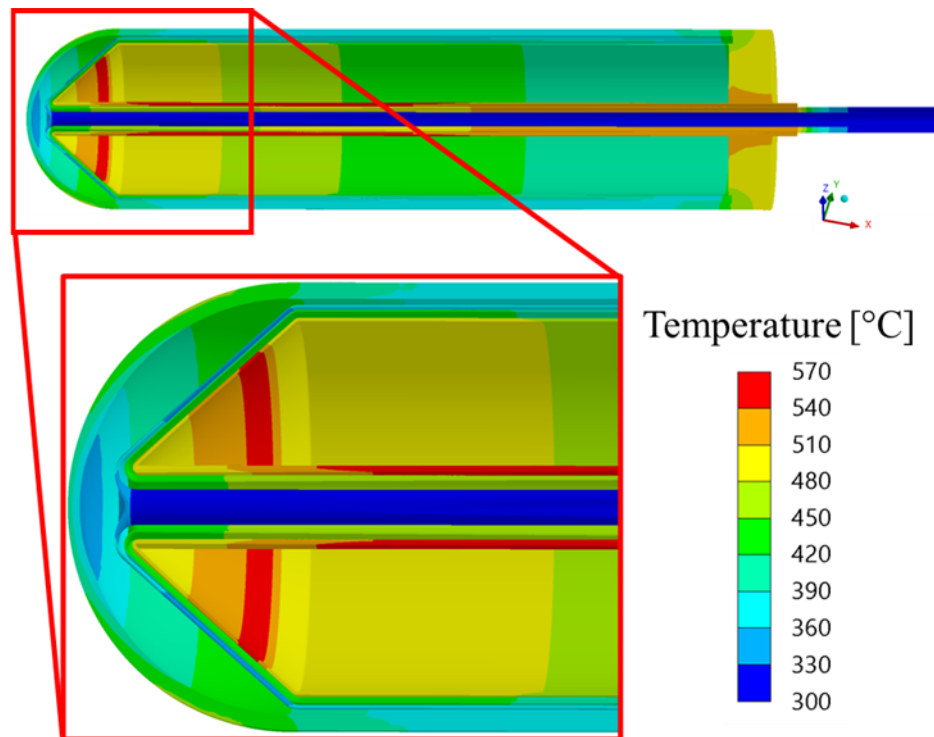


Figure 4.22: Pin 3D thermal field - case 2 (view in section) [47].

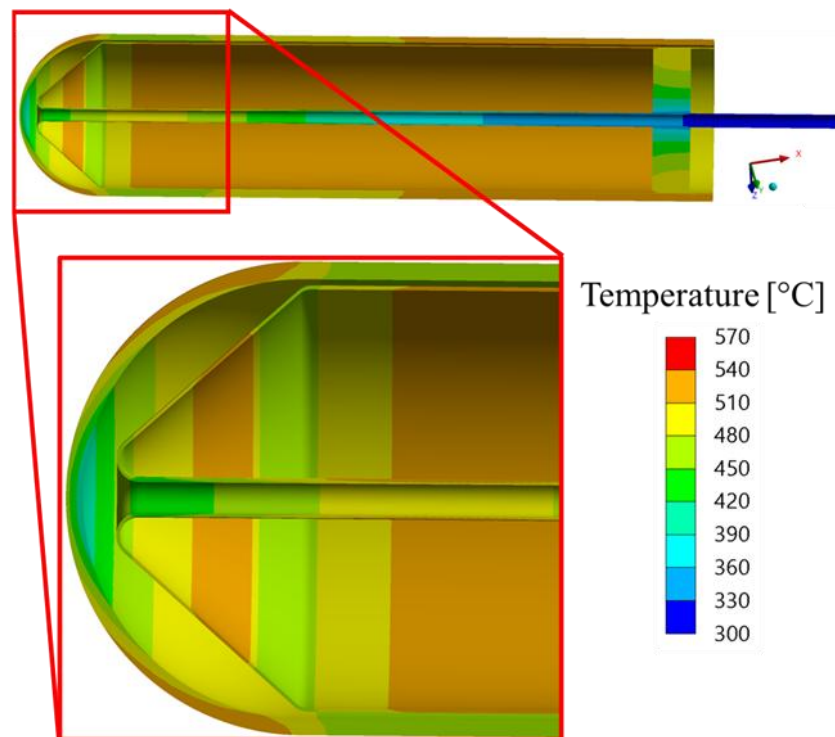


Figure 4.23: Pin 3D thermal field - case 5 (view in section) [47].

As to the boundary conditions, the displacement field calculated from the global model analysis has been mapped onto the pin external surfaces in contact with the rest of the

structure. In this way, it is possible considering the mechanical effect of the surrounding pins on the structural behaviour of the assessed one, as detailed when describing the submodeling technique (section 4.2). It is to be recalled that, as already mentioned, with the purpose to perform structural verifications according to the RCC-MRx standards [19], the sole knowledge of the total stress tensor $P+Q$ is not sufficient, but it is necessary to determine how it breakdowns into primary stresses P and secondary stresses Q . As formerly detailed in section 4.3.1, the analyses that have been setup are relevant to the determination of the total stresses and the secondary stresses, while the primary stresses have been calculated by means of a *solution combination* (Eq. 4.14). Hence, the displacement fields that have been applied in the submodel analysis were those relevant to the total stresses of the global model when calculating the total stresses of the submodel, and those relevant to the secondary stresses of the global model when setting up the analyses to determine the secondary stresses of the submodel.

In the following, the displacement fields corresponding to the total stresses are shown. In detail, regarding the nominal pressure load case, the displacement field mapped onto the pin external surface is represented in Figure 4.24, whereas the corresponding field obtained for design pressure case is depicted in Figure 4.25.

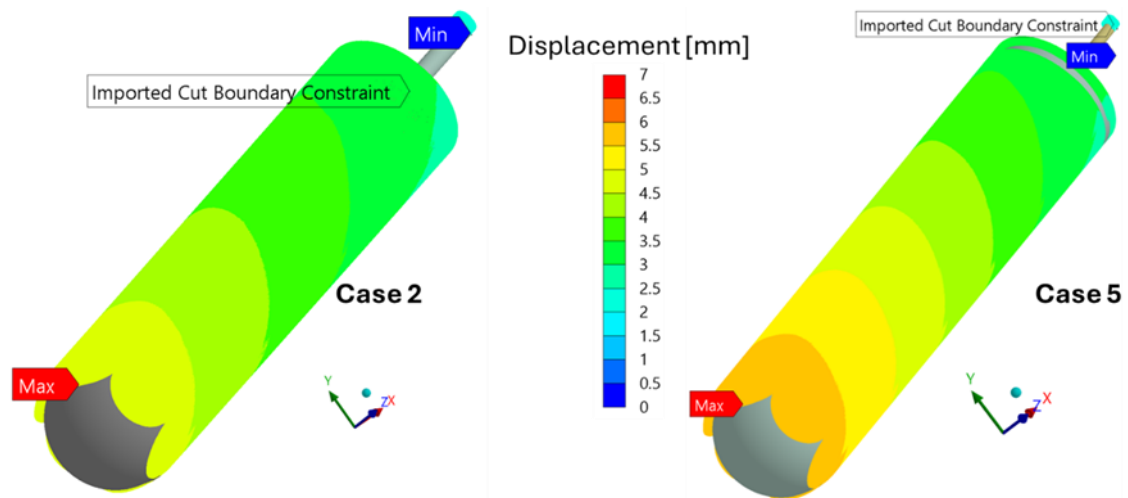


Figure 4.24: Nominal pressure - The imposed displacement field [47].

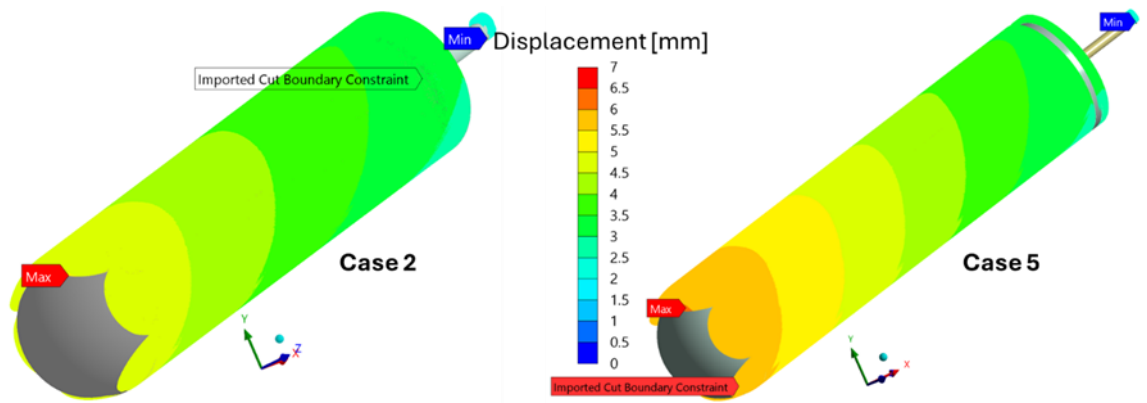


Figure 4.25: Design pressure - The imposed displacement field [47].

As it can be observed, the considered displacement fields are quite similar since the effect of the pressure variation is not so intense in terms of deformation of the pin external envelope.

As to the pressure load, it has been applied solely in the analyses aimed at the evaluation of the total stresses, and once again it has been imposed considering the nominal pressure and design pressure, envisaging 8.0 and 9.2 MPa, respectively for both purge gas and coolant [77]) have been considered as already done for global model analysis. Since the contact among beryllide blocks and the pin internals is not perfect because of small gaps and/or manufacturing imperfections allowing helium permeation, also these surfaces have been assumed subjected to the purge gas pressure.

4.4.2 Submodel analysis results

Once assigned the proper loads and boundary conditions, structural steady-state analyses have been run to assess the thermomechanical behaviour of an HCPB monoblock pin of the COB segment and perform structural verifications according to the RCC-MRx structural design code [19].

In the following, the Von Mises stress distribution is shown both for the nominal pressure (Figure 4.26 for case 2 and Figure 4.27 for case 5 layout) and for the design pressure load scenario (Figure 4.28 for case 2 and Figure 4.29 for case 5 layout).

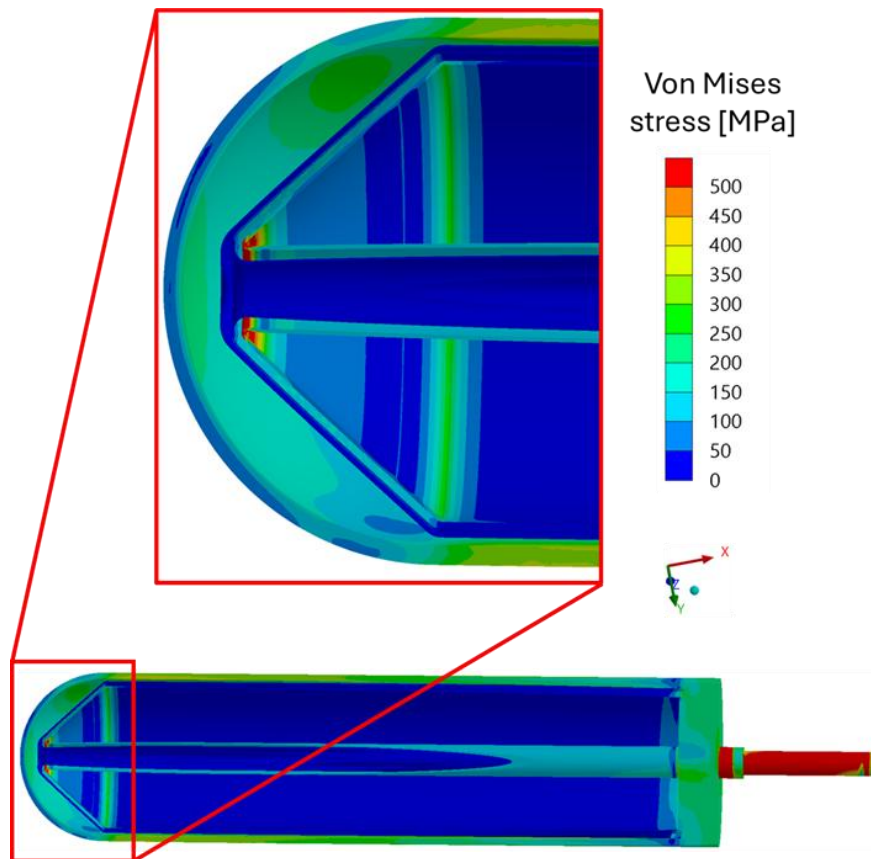


Figure 4.26: Submodel - Case 2 - Nominal pressure - Von Mises equivalent stress [47].

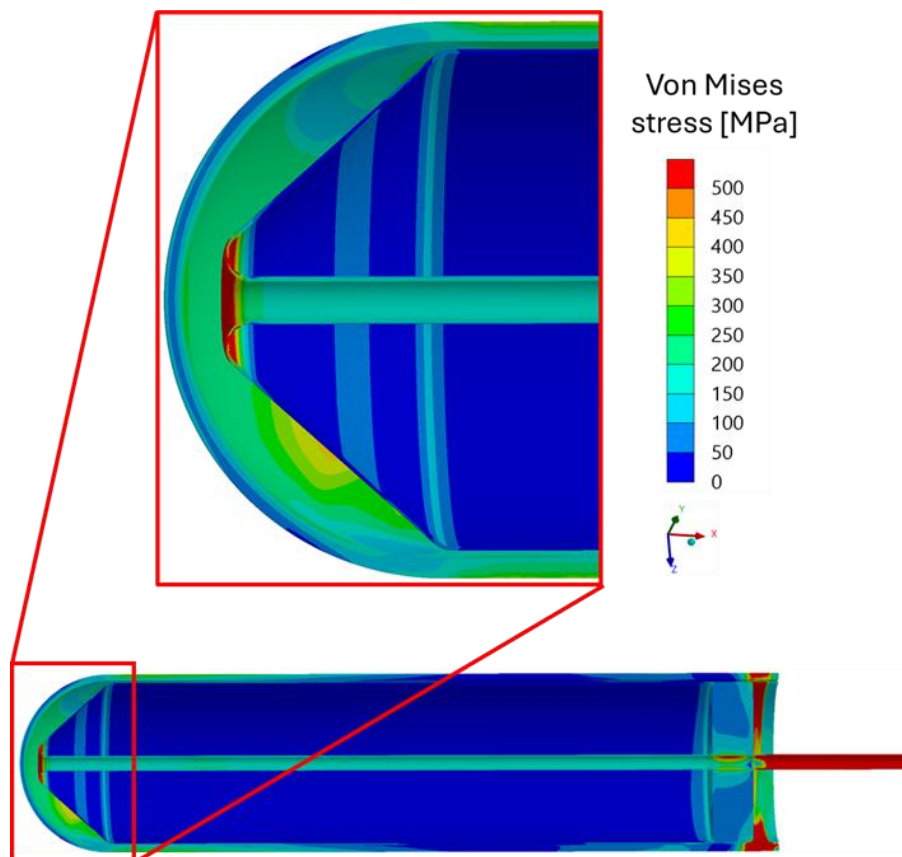


Figure 4.27: Submodel - Case 5 - Nominal pressure - Von Mises equivalent stress [47].

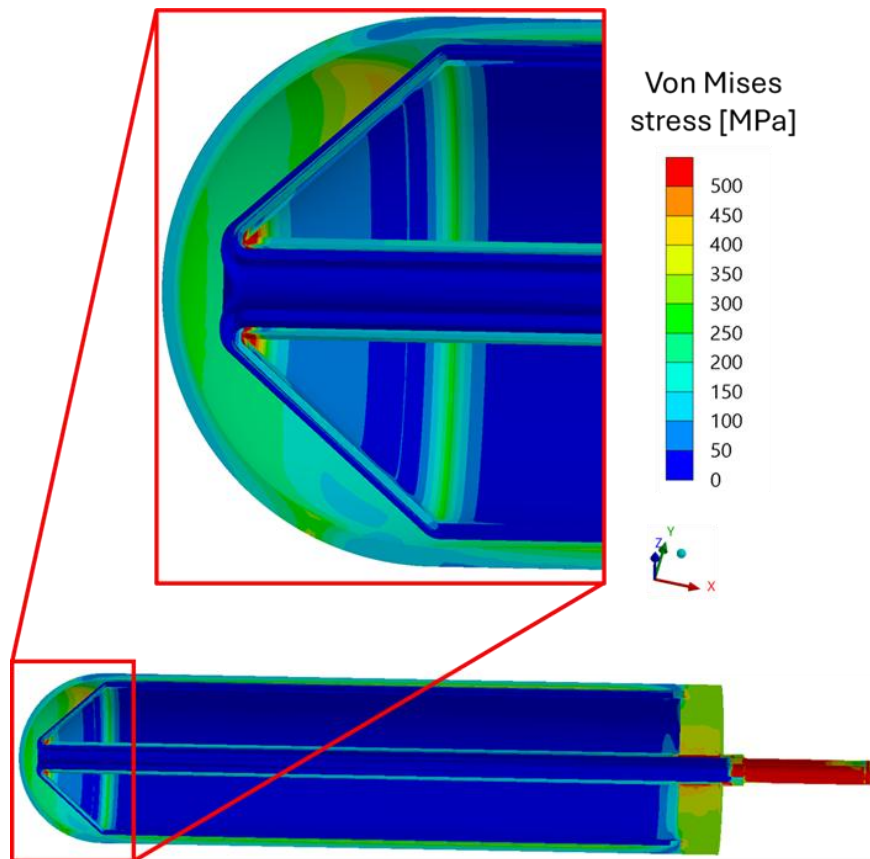


Figure 4.28: Sub model - Case 2 - Design pressure - Von Mises equivalent stress [47].

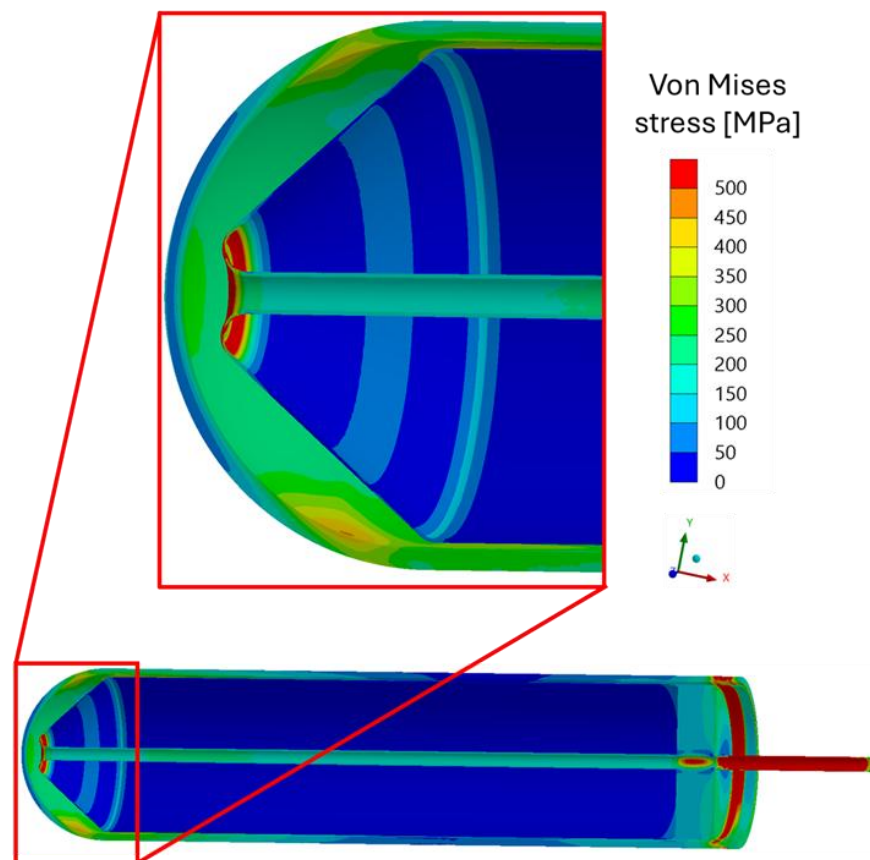


Figure 4.29: Sub model - Case 5 - Design pressure - Von Mises equivalent stress [47].

It can be observed that, for a given pin architecture, similar Von Mises stress distributions are obtained passing from nominal to design pressure scenario, indicating that the most significant contribution in terms of stress is given by the thermal field. In particular, in the design pressure scenario, higher stress is predicted in some specific areas, such as the plug, the bend region in correspondence of the coolant outlet, and some regions of the pin envelope. In agreement with the methodology described in section 4.2, a set of paths has been created for either pin design in order to check the fulfilment of the level A RCC-MRx [19] structural design criteria. It is hereby recalled that the paths have to be defined across the thickness of the regions that are most critically solicited. Figure 4.30 (case 2) and Figure 4.31 (case 5) illustrate the path locations superimposed to section views of the Von Mises stress fields calculated for the design pressure loading scenario. As it can be observed, no path has been constructed in case 2 within inner and outer metal sheets because of the low stress predicted. While the Von Mises stresses shown in the present section are relevant to the total stresses $P+Q$, it shall be recalled that also the analyses aimed at assessing the secondary stresses Q have been performed, and, finally, the primary stresses have been computed by a *solution combination* according to Eq. 4.14. The specific knowledge of the primary and secondary stresses has allowed to perform the design criteria verification.

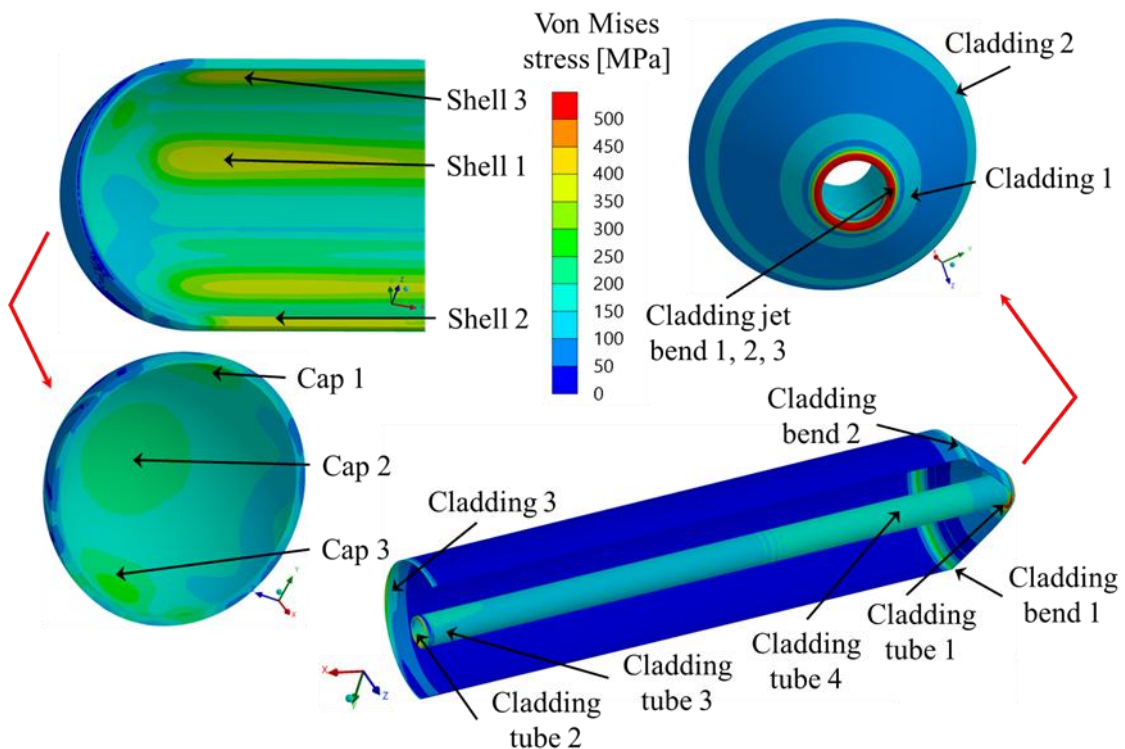


Figure 4.30: Case 2 - Path locations [47].

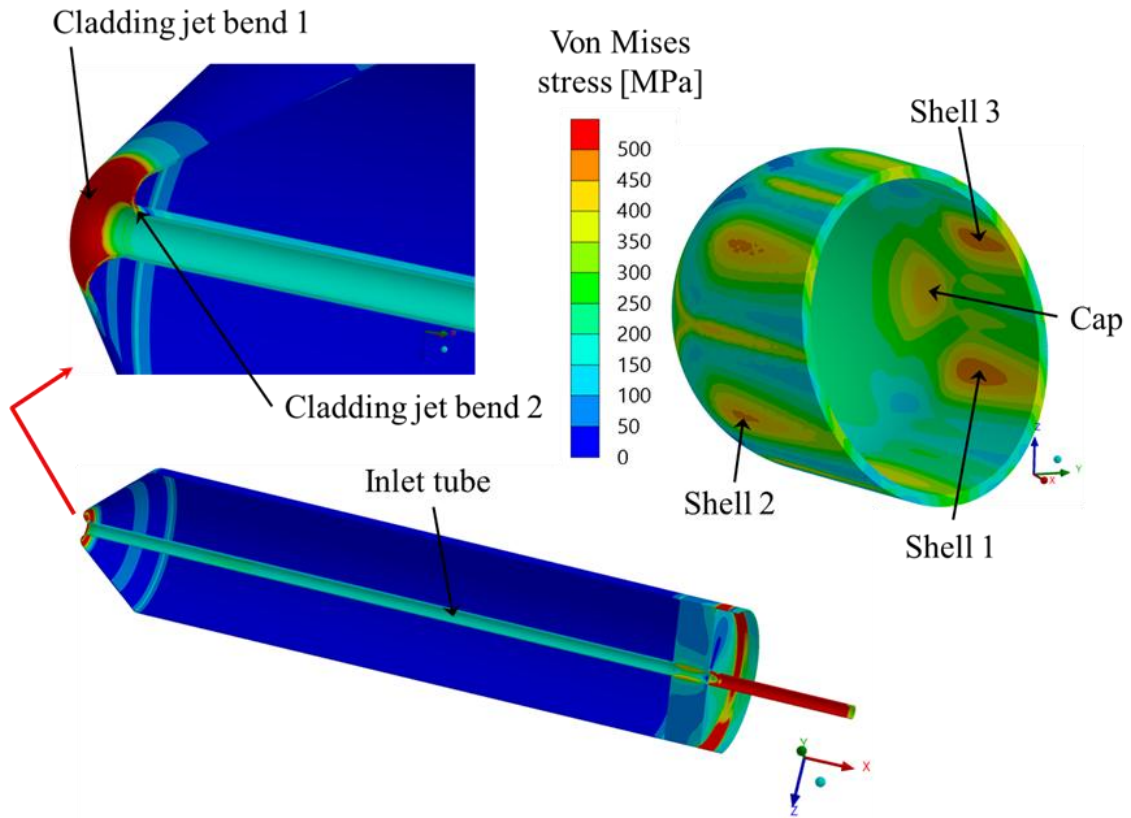


Figure 4.31: Case 5 - Path locations [47].

As widely detailed in section 4.2, the level A RCC MRx rules against Immediate Excessive Deformation, Immediate Plastic Instability, and Immediate Plastic Flow Localization have been considered, and the relevant utilization factors have been defined in Eq. 4.11, Eq. 4.12, and Eq. 4.13 respectively. The relevant stress limits have been evaluated at the path average temperature (T_{ave}). The IED and IPI criteria consider the primary load whereas the IPFL also considers the effect of the secondary load.

Regarding the case 2 layout, Table 4.2 summarizes the utilization factors evaluated for the three considered criteria both in the nominal and design working conditions, whereas Table 4.3 details the analogous quantities evaluated for the RCC-MRx criteria verification carried out on case 5. In both the tables, T_{ave} values in between 500 °C and 550 °C are highlighted in orange, as well as values exceeding 550 °C (predicted in case 2 only) are marked in red. Similarly, values of the utilization factors between 0.8 and 1 have been reported in orange, and values above unity have been marked in red.

Table 4.2: RCC-MRx criteria verification - case 2 layout.

Path	IED		IPI		IPFL		T_{ave} [°C]
	Nominal	Design	Nominal	Design	Nominal	Design	
Shell 1	0.48	0.55	0.44	0.50	1.05	1.08	392.1
Shell 2	0.55	0.63	0.39	0.45	1.10	1.14	397.4
Shell 3	0.50	0.57	0.45	0.52	1.06	1.09	392.8

Cap 1	0.65	0.75	0.47	0.54	0.61	0.59	440.5
Cap 2	0.43	0.49	0.32	0.37	0.24	0.24	375.8
Cap 3	0.82	0.95	0.65	0.75	0.68	0.65	441.2
Cladding bend 1	0.02	0.02	0.03	0.04	0.42	0.42	494.2
Cladding bend 2	0.02	0.02	0.02	0.02	0.4	0.40	493.6
Cladding jet bend 1	0.05	0.06	0.04	0.05	1.95	1.96	484.0
Cladding jet bend 2	0.04	0.04	0.04	0.05	1.35	1.35	484.0
Cladding jet bend 3	0.02	0.02	0.03	0.04	0.63	0.64	485.2
Cladding 1	0.01	0.01	0.01	0.01	0.37	0.38	495.5
Cladding 2	0.01	0.01	0.01	0.01	0.20	0.20	540.7
Cladding tube 1	0.01	0.01	0.01	0.01	0.41	0.41	489.5
Cladding tube 2	0.08	0.09	0.14	0.16	0.48	0.49	518.8
Cladding tube 3	0.06	0.07	0.10	0.11	0.41	0.41	519.0
Cladding tube 4	0.02	0.02	0.01	0.01	0.60	0.60	561.1
Cladding 3	0.40	0.45	0.41	0.48	0.47	0.50	438.3

Table 4.3: RCC-MRx criteria verification - case 5 layout.

Path	IED		IPI		IPFL		T _{ave} [°C]
	Nominal	Design	Nominal	Design	Nominal	Design	
Inlet tube	0.01	0.01	0.01	0.01	0.51	0.51	516.3
Shell 1	0.55	0.63	0.39	0.45	1.36	1.40	393.0
Shell 2	0.71	0.82	0.51	0.59	1.47	1.51	529.2
Shell 3	0.65	0.74	0.52	0.59	1.28	1.31	470.5
Cap	0.70	0.81	0.51	0.58	1.00	0.97	473.7
Cladding jet bend 1	0.02	0.02	0.05	0.05	0.75	0.75	471.4
Cladding jet bend 2	0.02	0.02	0.01	0.02	0.77	0.77	516.8

All the paths of both geometries fulfil the IED and IPI level A criteria, namely those considering solely the primary stresses. Moreover, comparing the results obtained under nominal and design pressure loading conditions, it can be noted that under design pressure some paths fulfil the rules with a narrower margin, which is especially noticeable for the path “Cap 3” of the case 2 layout, where the utilization factor for IED reaches the value of 0.95. However, given the intrinsic levels of conservatism in the RCC-MRx rules, it can be concluded that both pin architectures exhibit a rather satisfactory thermomechanical behaviour concerning the primary stresses in *operational* and *expected* (level A) DEMO working scenarios.

When also considering secondary stresses, and hence the influence of the thermal fields assessed and optimized in the thermofluid-dynamic analysis campaign (Chapter 3), the general picture becomes less ideal. In fact, multiple paths of either pin geometry do not fulfil the IPFL criterion.

As to the case 2 pin layout, when considering the pin envelope (paths located in the cap and shell), the IPFL utilization factors overcome unity by very small margins. Hence, given

the high level of conservatism of the criterion, these values should pose only minor concerns, and the overall structural behaviour of the pin envelope should still be considered quite promising. In this regard, it has to be recalled that stress limit values at 20 dpa are considered, introducing a further factor of conservatism that is probably too much stringent. On the other hand, when considering the pin internals, most of the paths (even those characterized by average temperatures approaching 550 °C) show rather low utilization factors, but a major violation and a moderate violation of the IPFL criterion emerge in the paths “cladding jet bend 1” and “cladding jet bend 2” respectively. These violations occur within a wide region where rather high Von Mises stresses are predicted (see Figure 4.30), therefore seem to arise from an actual criticality of the design rather than from any numerical issue or model approximation. Hence, this result suggests that for the case 2 layout (which, in this analysis, is representative of the most promising pin architectures envisaging a double coolant flow pass) a reshaping of the “cladding jet bend” region should be considered. It can be noticed that the corresponding area of the coolant flow path is not among the ones which significantly contribute to the determination of the total pressure drop (recall Table 3.20). Moreover, being the cladding jet bend located close to the X symmetry axis, a reshaping (say, for instance, adopting a higher curvature radius) should result in slight changes in the BZ volume. Additionally, this region is thermally insulated – by means of the stagnant helium layer – from the pin envelope. These considerations allow to conclude that the criticality occurring in the cladding jet bend can be specifically target in the double coolant flow pass geometries without the concern to compromise the thermal behaviour of the design in different domains nor its total pressure drop.

It can be finally observed that the criticality occurring in the cladding is in line with one of the trends emerged in the thermofluid-dynamic analysis campaign, that is, the critical thermal behaviour of the cladding of double coolant flow pass configurations, and the impossibility to perform a final decision on a single optimal pin geometry prior to knowing the results of further research on cladding materials. Hence, once again, the cladding emerges as the critical part upon which depends the viability of double coolant flow path pin architectures.

As to the case 5 pin layout, the pin internals do not show any violation of the IPFL criterion. When considering the envelope, the path in the cap has a utilization factor that reaches unity, which should not be deemed of particular concern given the high level of conservatism of the criterion. However, significant violations of the IPFL criterion are registered in the paths performed in the shell. The entity of the utilization factors (up to 1.51 in the path “shell 2” under design pressure loading conditions) suggests that the result is

caused by an actual severity of the loading conditions affecting the envelope of the case 5 pin layout, which is hereby representative of the most promising pin architectures envisaging a single coolant flow pass. Indeed, the overall thermomechanical behaviour of the case 5 layout reflects the underlying idea of the single flow pass architecture, that is, sacrificing the thermal behaviour of the pin envelope to achieve a less severe thermal loading of the pin internals (and a lower coolant pressure drop). However, observing Figure 4.31, it can be noticed that the paths performed in the shell are situated in localized areas exhibiting rather higher Von Mises stresses than the surroundings. Consequently, further thermomechanical studies may reveal cooperation mechanisms between the most severely stressed regions and the surroundings that allow the pin to operate compatibly with the RCC-MRx level A standards. Moreover, should it not be the case, still the localized nature of the critical regions suggests that the issue could disappear once determined an optimized configuration of the manifold region of the HCPB monoblock BB, or, further, with a minor intervention on the arrangement of the pins within the BB. Hence, considering once again the high levels of conservatism of the considered criterion (included stress limit at 20 dpa), it can be concluded that the criticality emerged in the shell of the case 5 layout cannot be deemed – at the present stage of knowledge – a showstopper for the single coolant flow pass pin architectures.

One last outcome of the submodel analysis that is worth attention is relevant to the displacement field observed in the case 2 pin layout. In more detail, attention has been paid to the axial displacement of the internal metal sheets, shown in Figure 4.32. Indeed, the initial distance in between the two metal sheets is of 1 mm, and it has been observed that, under the applied loads, the differential deformation generates interferences.

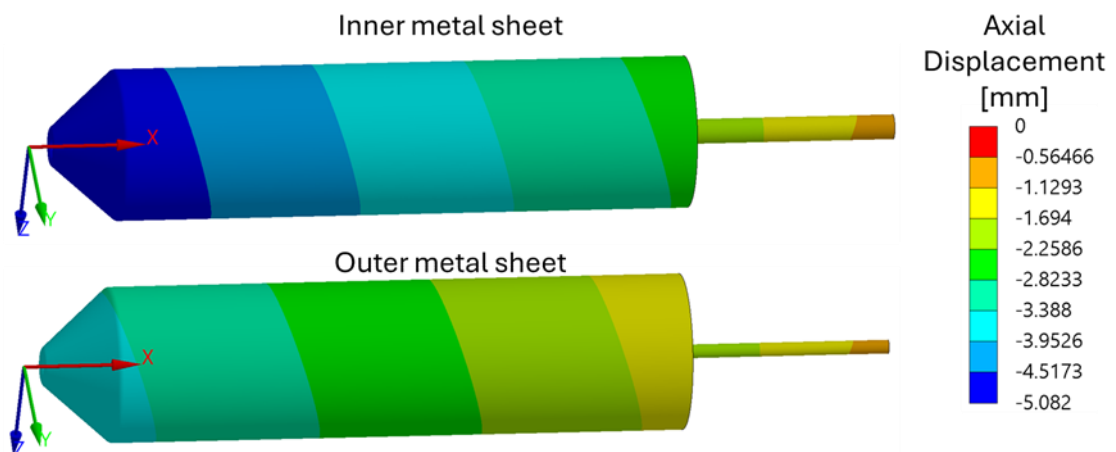


Figure 4.32: Case 2 – Design pressure – Metal sheets axial displacement [47].

Specifically, observing Figure 4.32 it can be deduced that the inner metal sheet moves backward along the pin axis (namely the X direction shown in figure) more than the outer

sheet, causing an overlapping in between the two. Although the displacements are shown for the design pressure scenario, also in the case of nominal pressure the initial gap is almost totally closed by the different axial movement of the two sheets (the inner moving towards negative X more than the outer). Since no contact models have been implemented in the FEM models, the mutual exchange of forces due to the contact is not taken into account in this study. So it could be that the system achieves an equilibrium configuration with a small gap in between the sheets, at the cost of an increased stress. In any case, since the presence of the stagnant helium layer is a key feature of double coolant flow pass geometries that significantly affects its thermal behaviour, it should be considered to equip the metal sheets with spacers. This is a further complication of the double coolant flow pass layouts that is not, in turn, present in the simpler designs of the single coolant flow pass pin configurations.

4.5 Grid independence assessment

In order to verify that the illustrated results are not affected by excessively coarse domain discretization, a comprehensive grid independence analysis has been performed. The ultimate outcome of the exploratory thermomechanical analysis is the evaluation of the utilization factors on the most solicited regions of the pin domains. Since the submodeling technique has been adopted (as detailed in section 4.2), these utilization factors are influenced by both the discretization of the global model and that of the submodel. Hence, the mesh independence evaluation consisted in a comparison of the results obtained within the pin domains (submodels) when adopting different meshes for both the submodel and the global model employed to impose the relevant boundary conditions. Moreover, the utilization factors are affected by the thermal field both via the path-average temperature, which influences the stress limits, and, for criteria that include secondary stress tensors, through the impact of the thermal field on the secondary stresses. In this regard, it is hereby recalled that the thermal field in the submodel is inferred from the CFD analyses by means of the following mapping procedures: as to the pin internals, the thermal field of the CFD analysis is directly mapped onto the submodel, while, as to the pin envelope, the thermal field of the CFD analysis is firstly mapped onto the global model, and, subsequently, from the global model to the submodel. Hence, despite the temperature distribution is inferred from the CFD analyses, still the accuracy of the thermal field adopted in the FEM studies is affected by the node density of both the global and the local model, as it influences the interpolation distances involved in the mapping process. To account for this aspect, both the primary and the secondary loads have been considered in the grid independence assessment. Regarding the pressure load, it has been arbitrarily decided to perform the comparison under

the design pressure. Since the analyses detailed in the previous sections have been conducted with the same global model for both the case 2 and case 5 pin layouts, and with the same element size for both case 2 and case 5 mesh, the case 2 design has been selected to perform the evaluations on grid independence due to its higher geometrical complexity. In fact, case 2 is characterized by the same Eurofer domains of the case 5 with the addition of the Eurofer sheets, therefore the grid independence of the results of case 5 layout can be inferred by that of the case 2 geometry, whereas the opposite would not be possible.

Five analyses have been compared to assess the mesh independence, adopting 5 different discretizations for both the submodels and the relevant global models. For either, the mesh element size is progressively halved passing from the analysis envisaging the coarsest models (named D1), to that adopting the most refined meshes (D5). The results of the analysis D4 are those presented in section 4.4.2, and therefore are also indicated as “case study”. For consistency of the grid independence evaluation, all the analyses adopted the same element topologies as the case study D4 analysis, namely hexahedral and tetrahedral quadratic elements for the global model, and tetrahedral quadratic elements for the submodel. Table 4.4 summarizes the features of the calculation grids adopted for the mesh independence analysis.

Table 4.4: Summary of discretization features for grid independence analysis.

Analysis		D ₁	D ₂	D ₃	D ₄ (case study)	D ₅
Global model	Element size [mm]	56	28	14	7	3.5
	Number of nodes	$0.894 \cdot 10^5$	$1.34 \cdot 10^5$	$4.61 \cdot 10^5$	$27.5 \cdot 10^5$	$200 \cdot 10^5$
	Number of elements	$0.463 \cdot 10^5$	$0.614 \cdot 10^5$	$2.08 \cdot 10^5$	$12.8 \cdot 10^5$	$99.8 \cdot 10^5$
Submodel	Element size [mm]	12	6	3	1.5	0.75
	Number of nodes	$3.35 \cdot 10^5$	$5.26 \cdot 10^5$	$20.5 \cdot 10^5$	$47.5 \cdot 10^5$	$213 \cdot 10^5$
	Number of elements	$1.68 \cdot 10^5$	$2.42 \cdot 10^5$	$9.01 \cdot 10^5$	$20.1 \cdot 10^5$	$104 \cdot 10^5$
Total number of nodes		$4.25 \cdot 10^5$	$6.60 \cdot 10^5$	$25.1 \cdot 10^5$	$75.0 \cdot 10^5$	$413 \cdot 10^5$
Total number of elements		$2.14 \cdot 10^5$	$3.04 \cdot 10^5$	$11.1 \cdot 10^5$	$32.8 \cdot 10^5$	$204 \cdot 10^5$

Firstly, a node-by-node comparison of the stress tensor components within the pin domain has been performed. In more detail, the spatial distributions of every stress tensor component obtained in the analyses D₁ through D₄ have been mapped onto the nodes of the D₅ calculation grid. Subsequently, at each node, the percentage difference between every stress

component from the considered analysis and the corresponding component from the D₅ analysis – taken as the most accurate reference – has been computed according to Eq. (4.15).

$$\Delta_{ijD_k} = \frac{(\sigma_{ij})_{D_k} - (\sigma_{ij})_{D_5}}{(\sigma_{ij})_{D_5}} \quad (4.15)$$

Where k varies between 1, 2, 3 and 4 based on the considered analysis.

In order to minimise the background noise given by high percentage variations on small values, it has been decided to filter out the nodes where the stress component of D₅ is comprised between ± 10 MPa. It is worth pointing out that, due to the tensorial nature of the stress field and the presence of both positive and negative stress components, the signs of the percentage differences evaluated by Eq. 4.15 do not provide any information on the relative severity of the compared stress fields.

The so-obtained percentage differences have been postprocessed to visualize the distribution of the numerical errors through histograms and cumulative curves, as visible in Figure 4.33. Observing the cumulative curves, it is noticeable that in the D₁ analysis all the stress components except σ_x and σ_{xy} have a negative percentual error beyond -100% in more than half of the nodes, indicating a major disagreement between the results obtained with the coarsest and the most refined (D₅) meshes. The share of nodes presenting negative errors beyond -100% becomes small when considering the D₂ analysis, and negligible when observing D₃ and D₄. On the other hand, positive errors beyond 100% are only observed in a small share of the nodes in the D₁ analysis. Observing the histograms, it is evident that transitioning from the coarser to the more refined mesh results in a narrower distribution centered around 0% error. In more detail, the frequency of occurrence of errors comprised between $\pm 5\%$ (central bin of the histogram) is observed to raise monotonically when transitioning to finer discretizations. In this regard, it is relevant to point out that, for every stress tensor component, more than half of the nodes in analyses D₃ and D₄ exhibit percentage deviations within the range $\pm 5\%$, indicating that both cases show a very high level of agreement with the results of the D₅ analysis.

Based on the illustrated trends, it can be concluded that the models adopted in the case study D₄ analysis are characterized by meshes that are refined enough to ensure mesh-independent results. In fact, the stabilization of the solution appears to be reached at some point between cases D₂ and D₃, suggesting that the results illustrated in this work lie within the range of grid independence with an abundant margin, since obtained with a significantly more refined model than D₃.

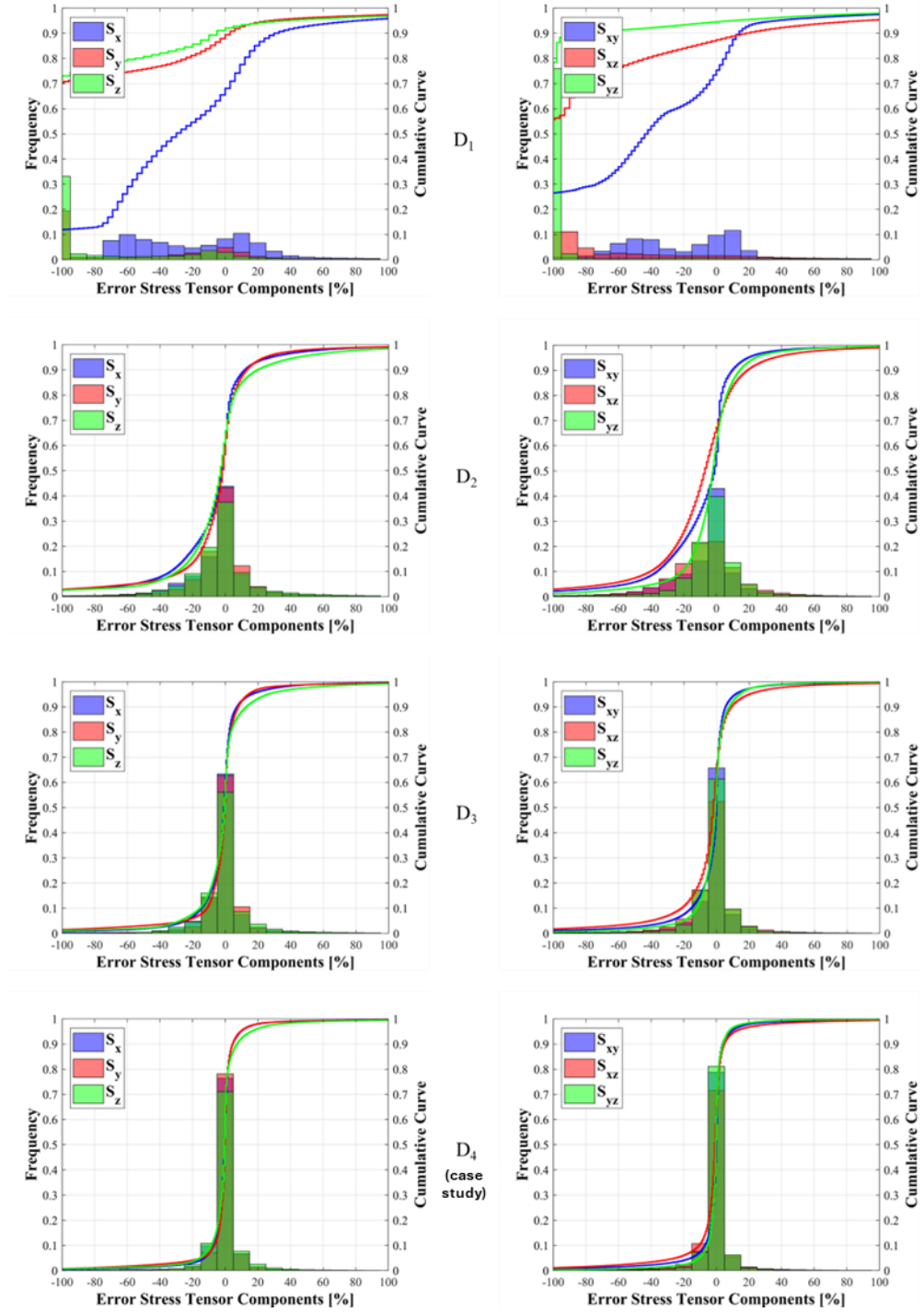


Figure 4.33: Percentage error distribution on normal (left) and shear (right) stress components, for analyses D₁ to D₄ (top to bottom).

A further verification of the grid independence has been carried out by computing the utilization factors of the IPFL criterion in the 18 paths shown in Figure 4.30. In more detail, the IPFL criterion has been chosen among the three adopted criteria, by virtue of being the one exhibiting the highest usage factors, and, consequently, the most relevant in drawing the conclusions of the thermomechanical study. Moreover, this criterion involves both primary and secondary stresses (recall Eq. 4.13) and thus constitutes a comprehensive indicator of

the numerical errors arising from mesh density. In each path, the usage factor obtained in the considered analysis has been compared to that of the D₅ analysis, and the absolute value of the percentage deviation has been evaluated by Eq. 4.16.

$$\Delta_{i_{D_k}} = \left| \frac{(UF_i)_{D_k} - (UF_i)_{D_5}}{(UF_i)_{D_5}} \right| \quad (4.16)$$

Where i identifies the considered path among the 18 shown in Figure 4.30, and k varies between 1, 2, 3 and 4 based on the considered analysis. For each analysis, it has been computed the average value of the 18 percentual differences evaluated in each path through Eq. 4.16, so to obtain a single value representative of the overall disagreement in the evaluation of the utilization factors compared to the most refined D₅ analysis. Finally, the complement to 100% of this error is plotted in Figure 4.34 against the total number of nodes (Table 4.4) in order to ease the visualization of the numerical convergence on estimation of the utilization factors.

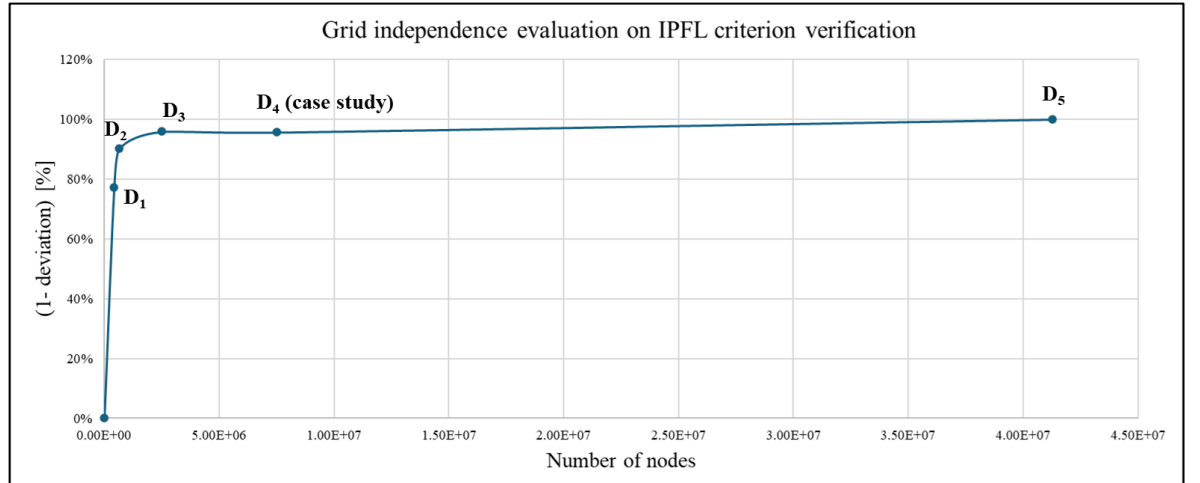


Figure 4.34: Numerical convergence on evaluation of IPFL utilization factors.

The trend observed in Figure 4.34 shows that also when considering the estimation of the utilization factors, the stabilization of the results appears to be reached at some point between cases D₂ and D₃, and hence the case study D₄ analysis lies within the range of grid independence with an abundant margin. It can be stated that the results discussed in the present chapter are not negatively affected by excessively coarse domain discretization.

4.6 Conclusions of the thermomechanical analysis

The exploratory thermomechanical analysis on the HCPB monoblock BB has been focused on the study of two different pin configurations, assumed to be located in the COB segment. Specifically, the case 2 and case 5 pin layouts have been assessed, representing the geometries characterised by the lowest Eurofer temperatures among the double and single coolant flow pass architectures respectively (Chapter 3). Moreover, for the thermal loads,

the especially favourable condition envisaging HF_0 of 0.3 MW/m^2 , ΔT of $220 \text{ }^\circ\text{C}$, and roughness on machinable surfaces of $200 \text{ }\mu\text{m}$ has been considered, so to draw conclusions on the HCPB monoblock BB pin structural behaviour under the most favourable conditions.

The first part of the study focused on a global model representing the equatorial slice of the HCPB monoblock COB segment, with the scope to obtain realistic BCs to be imposed for the subsequent submodel analysis focused on a single pin. These analyses have shown that the honeycomb configuration is robust enough to keep the pins in their mutual positions without presenting any region with concerning Von Mises stress values.

The subsequent submodel analyses has shown that both the case 2 and the case 5 pin layout fulfil the RCC-MRx [19] level A criteria against IED and IPI, which only consider the primary stress tensor. However, both the pin arrangements present regions that do not comply with the rule against IPFL, which also considers the secondary stresses, showing that the thermal fields of the Eurofer pin domains play a fundamental role in the thermomechanical performance of the HCPB monoblock pin.

In more detail, the case 2 layout exhibits a rather promising behaviour in the regions of the pin envelope, where the IPFL utilization factor only overcomes unity by very small margins, that pose small concern when considering the high conservatism of the RCC-MRx rule. However, the pin internals of the case 2 geometry present a major violation of the IPFL rule (a utilization factor of 1.96), that occurs in a wide region where high Von Mises Stresses are predicted in the Eurofer cladding. This result suggests the need for a reshaping of the “cladding jet bend” region in the double coolant flow pass geometries. Indeed, considering the trends emerged in the thermofluid-dynamic campaign (Chapter 3), reshaping the critical region seems feasible without any significant consequence on the thermal behaviour in other parts of the pin and on the pressure drop. Finally, the analysis has shown the necessity to equip the metal sheets with spacer so to maintain the stagnant helium gap despite the differential axial deformation.

As to the case 5 layout, the IPFL criterion is fulfilled in the paths located in the pin internals, while some moderate violations appear when considering the pin envelope. In more detail, three paths in the Eurofer shell present utilization factors higher than unity, with a maximum of 1.51 reached under design pressure loading conditions. However, these violations are verified in regions characterised by localized stress concentrations, hence more detailed studies may reveal that they pose no concern by virtue of cooperation mechanisms of the critical zones with the surroundings, or that they disappear when considering a proper manifold region optimized for the HCPB monoblock BB. Moreover,

minor modifications of the pin arrangement may probably cancel the stress concentration and allow the fulfilment of the level A criterion against IPFL.

In general terms, it can be concluded that the exploratory thermomechanical study shows rather encouraging results for the structural viability of the HCPB monoblock pin, because, although the RCC-MRx level A criteria have not been totally fulfilled, there appears to be abundant room for improvement without compromising the satisfactory thermal and fluid-dynamic behaviour of the optimized layouts. Moreover, the comparison between the double and single coolant flow pass arrangements aligns with the trends observed in Chapter 3 , with the single pass option that benefits from a simpler design and sacrifices the behaviour of the pin envelope to reduce loading on the pin internals and, conversely, the double pass that optimizes the pin envelope's performance but undergoes the detrimental effects of higher geometrical complexity and remains dependent on further developments of the cladding.

Conclusions

The research activities carried out during the XXXVIII Cycle PhD course in Energy, curriculum Low Carbon Energetics and Innovative Nuclear Systems – held at the University of Palermo – are framed within the R&D effort supported by the EUROfusion consortium to advance the design of the DEMO breeding blanket, which is a core reactor component currently affected by a low technology readiness level.

The Thesis work addressed for the first time the thermofluid-dynamics and thermomechanics of an innovative breeding blanket concept, specifically a variant of the reference Helium-Cooled Pebble Bed BB designed to overcome its main issues regarding manufacturing, reliability, and safety.

The analysed concept, named HCPB monoblock, envisages a monoblock architecture composed of modular pins that contain all the functional materials. The research work was articulated in an extensive optimization campaign focused on the thermofluid-dynamics of the HCPB monoblock pin, and on an exploratory study of its thermomechanical behaviour. No dedicated neutronic analyses have been executed, and the nuclear heating distributions of a similar concept have been adopted.

To perform a comprehensive optimization campaign, a 2D axisymmetric model of the HCPB monoblock pin has been created from scratch and implemented on the commercial CFD code Ansys Fluent, endowing to run over 350 thermofluid-dynamic simulations with a manageable computational burden. With this FVM-based approach, the steady-state thermal and fluid dynamic performance of several design options for the pin internals has been assessed under a variety of conditions in terms of radiative heat flux intensity and coolant temperature rise. Moreover, different values of roughness have been set on machinable coolant-wetted surfaces, showing significant benefits of higher roughness (up to 200 μm) on structural material temperatures.

The baseline pin layout envisages a double passage (back and forth) of the coolant within the pin. However, the criticalities emerged in the early stages of the optimization inspired the idea of a different kind of design – which quickly proved to be promising – characterized by a single coolant flow pass.

While all the simulations executed on double coolant flow pass layouts predict cladding peak temperatures exceeding 550 °C, thus suggesting that R&D on alternative cladding materials than Eurofer97 plays a crucial role in the attractiveness of this pin architecture, multiple layouts envisaging a single flow pass exhibit Eurofer97 temperatures below 550 °C under a coolant temperature rise of 220 °C, and a peak radiation load of 0.3 MW/m², with comparatively low coolant pressure losses. Additionally, the single pass options also offer the benefits of a significantly wider BZ, supposedly yielding higher TBR, and of a simpler geometry, generally leading to higher concept reliability and less demanding manufacturing. Nevertheless, only double flow pass arrangements with an updated cladding material may, potentially, withstand more intense radiation loads (up to 0.65 MW/m²) or a coolant temperature rise beyond 220 °C.

At the end of the thermofluid-dynamic optimization, multiple design options have been identified that either comply with all the requirements in terms of operational material temperatures and coolant pressure drop or only overcome the temperature limits by narrow margins. Specifically, the optimal geometries are both single and double coolant flow pass configurations envisaging different dimensions of the inlet tube radius, spanning from 5 mm to 8 mm. The availability of several design alternatives with diverse and robust thermofluid-dynamic performances emerges as a key strength of the HCPB monoblock concept.

The two pin geometries exhibiting the lowest structural material temperatures with an allowable coolant pressure drop among the double and single coolant flow pass architectures have been selected for the exploratory thermomechanical study, with the aim to assess the HCPB monoblock pin viability from the structural standpoint. To this end, the relevant 3D FEM models have been setup and implemented in Ansys Mechanical, where they have been assessed through a submodeling technique.

Hence, a preliminary analysis has been performed on a global model representing the equatorial slice of the HCPB monoblock COB segment, with the scope to obtain realistic BCs to be imposed for the subsequent submodel analysis focused on a single pin. This study demonstrated that the honeycomb configuration is robust enough to keep the pins in their mutual positions without presenting any region with concerning Von Mises stress values.

The subsequent submodel analyses indicated that both the pin arrangements fulfil the RCC-MRx level A criteria against IED and IPI, which only consider the primary stress tensor. However, both the pin configurations present regions that do not comply with the rule against IPFL, which also considers the secondary stresses, showing that the thermal response plays a fundamental role in the structural performance of the HCPB monoblock.

Nevertheless, the critical region of the double coolant flow pass layout – that is a low radius curvature in the cladding – can be reshaped without any significant consequence on its thermofluid-dynamics, while the critical area of the single flow pass design is rather localized and may thus be deemed of no concern by more in-depth thermomechanical studies, or may disappear when considering a manifold region optimized for the HCPB monoblock, or also a slight change in the honeycomb configuration.

In conclusion, the thermofluid-dynamic investigation has demonstrated the thermal and fluid dynamic viability of the HCPB monoblock, showing a quite satisfactory behaviour and the availability of multiple robust pin design options in view of the next design stages. Conversely, the exploratory thermomechanical study has not definitively proved the concept's structural viability but shows rather encouraging results as it highlights localized criticalities that can be addressed without compromising the favourable thermal and fluid-dynamic performance of the optimized layouts.

References

- [1] Copernicus Climate Change Service, [Online]. Available: <https://climate.copernicus.eu/surface-air-temperature-august-2025>.
- [2] Copernicus Climate Change Service, [Online]. Available: <https://climate.copernicus.eu/copernicus-2024-first-year-exceed-15degc-above-pre-industrial-level>.
- [3] I. E. Agency, “World Energy Outlook 2024”.
- [4] Donné, T.; Morris, W. European Research Roadmap to the Realisation of Fusion Energy; EUROfusion: Munich, Germany, 2018; ISBN 978-3-00-061152-0.
- [5] International Atomic Energy Agency, Considerations of Technology Readiness Levels for Fusion Technology Components, Vienna, 2024.
- [6] R. Kamendje et al., «Fusion Energy: Towards Feasible Power Plant Designs,» *Springer, Cham*, vol. Design Tools and Methods in Industrial Engineering III.
- [7] F. A. Hernández et al., “Advancements in Designing the DEMO Driver Blanket System at the EU DEMO Pre-Conceptual Design Phase: Overview, Challenges and Opportunities,” *Journal of Nuclear Engineering*, vol. 4(3), 2023.
- [8] A. Del Nevo et al., «Recent progress in developing a feasible and integrated conceptual design of the WCLL BB in EUROfusion project,» *Fusion Engineering and Design*, vol. 146, pp. 1805-1809, 2019.
- [9] P. Arena et al., «The DEMO Water-Cooled Lead–Lithium Breeding Blanket: Design Status at the End of the Pre-Conceptual Design Phase,» *Applied Sciences*, vol. 11(24), 2021.
- [10] P. Arena et al., «Design and integration of the EU-DEMO water-cooled lead lithium breeding blanket,» *Energies*, vol. 16(4), p. 2069, 2023.
- [11] G. Zhou et al., «The European DEMO Helium Cooled Pebble Bed Breeding Blanket: Design Status at the Conclusion of the Pre-Concept Design Phase,» *Energies*, vol. 16(14), 2023.

- [12] F. A. Hernández et al., «Consolidated design of the HCPB breeding blanket for the pre-conceptual design phase of the EU DEMO and harmonization with the ITER HCPB TBM program,» *Fusion Engineering and Design*, vol. 157, n. 111614, 2020.
- [13] G. Zhou et al., «A Water cooled Lead Ceramic Breeder blanket for European DEMO,» *Fusion Engineering and Design*, vol. 168, n. 112397, 2021.
- [14] G. Federici et al., « DEMO Design Activity in Europe: Progress and Updates,» *Fusion Engineering and Design*, vol. 136, pp. 34-41, 2018.
- [15] L.V. Boccaccini et al., «Status of maturation of critical technologies and systems design: breeding blanket,» *Fusion Engineering and Design*, vol. 179, n. 113116, 2022.
- [16] P. A. Di Maio et al., « Thermofluid-dynamic and thermal–structural assessment of the EU-DEMO WCLL “double bundle” Breeding Blanket concept left outboard segment,» *Fusion Engineering and Design*, vol. 202, 2024.
- [17] S. Giambrone et al., «Preliminary thermo-mechanical assessment of the Top Cap region of the Water-Cooled Lead-Ceramic Breeder Breeding Blanket alternative concept,» *Fusion Engineering and Design*, vol. 200, 2024.
- [18] ANSYS Inc., ANSYS Fluent User's Guide, Release 2023 R2, Canonsburg, PA, USA, 2023.
- [19] 2015 Afcen RCC-MRx Code.
- [20] ANSYS Inc. , MECHANICAL, Release 2023 R2, Canonsburg, PA, USA, 2023.
- [21] G. A. Spagnuolo et al., “Integrated multi-physics design tool for fusion breeding blanket systems - development and validation,” Karlsruhe: KIT-Bibliothek2020.
- [22] J. R. Lamarsh and A. J. Baratta, Introduction To Nuclear engineering, Prentice Hall, 2001.
- [23] A. A. Harms, D. R. Kingdon, K. F. Schoepf and G. H. Miley, Principles of Fusion Energy: An Introduction to Fusion Energy for Students of Science and Engineering, World Scientific Publishing Co Pte Ltd, 2000.
- [24] F. F. Chen, Introduction to Plasma Physics and Controlled Fusion, Springer, 2016.
- [25] L. A. El-Guebaly, “Fifty Years of Magnetic Fusion Research (1958-2008): Brief Historical Overview and Discussion of Future Trends,” *energies*, 2010.

- [26] S. Dubbioso et al., «Vertical stabilization of tokamak plasmas via extremum seeking,» *IFAC Journal of Systems and Control*, vol. 21, n. 100203, 2022.
- [27] G. Federici et al., «An overview of the EU breeding blanket design strategy as an integral part of the DEMO design effort,» *Fusion Engineering and Design*, vol. 141, p. 30–42, 2019.
- [28] G. Federici et al., «Introduction,» *Fusion Engineering and Design*, vol. 178, p. 113102, 2022.
- [29] C. Bachmann, «PDD - Plant Description Document,» EUROfusion, Munich, Germany, 2020.
- [30] A. Burlando et al., «EU DEMO pulsating balance of plant: Power conversion system operated with no thermal energy storage,» *Fusion Engineering and Design*, 2025.
- [31] E. Vallone et al., «Thermal-hydraulic study of the EU-DEMO Helium Cooled Pebble Bed Breeding Blanket Primary Heat Transport System,» vol. 2685, n. 012023, 2024.
- [32] R. Mitteau et al., «A shaped First Wall for ITER,» *Journal of Nuclear Materials*, vol. 415, pp. 969-972, 2011.
- [33] M. Dalle Donne et al., «Conceptual Design of Two Helium Cooled Fusion Blankets (Ceramic and Liquid Breeder) for INTOR,» *Kernforschungszentrum: Karlsruhe, Germany*, 1983.
- [34] M. Dalle Donne et al., «Status of EC Solid Breeder-Blanket Designs and R&D for DEMO Fusion Reactors,» *Fusion Engineering and Design*, vol. 27, pp. 319-336, 1995.
- [35] L. V. Boccaccini et al., «Design description and performance analyses of the European HCPB test blanket system in ITER feat,» *Fusion Engineering and Design*, vol. 61 and 62, pp. 339-344, 2002.
- [36] D. Carloni et al., «Advancement in HCPB DEMO Blanket Design,» in *Proceedings of the 28th Symposium on Fusion Technology (SOFT 2014)*, San Sebastian, Spain, 29 September-3 October 2014.
- [37] F. A. Hernández et al., «Overview of the HCPB Research Activities in EUROfusion,» *IEEE Transactions on Plasma Science*, vol. 46, pp. 2247-2261, 2018.

- [38] F. A. Hernández et al., «A New HCPB Breeding Blanket for the EU DEMO: Evolution, Rationale and Preliminary Performances,» *Fusion Engineering and Design*, vol. 124, pp. 882-886, 2017.
- [39] G. Zhou et al., «Preliminary Structural Analysis of the New HCPB Blanket for EU DEMO Reactor,» *International Journal of Hydrogen Energy*, vol. 41, pp. 7053-7058, 2016.
- [40] G. Zhou et al., «Design Study on the New EU DEMO HCPB Breeding Blanket: Thermal Analysis,» *Progress in Nuclear Energy*, vol. 98, pp. 167-176, 2017.
- [41] F. A. Hernández et al., «Advancements in the Helium-Cooled Pebble Bed Breeding Blanket for the EU DEMO: Holistic Design Approach and Lessons Learned,» *Fusion Science & Technology*, vol. 75, pp. 352-364, 2019.
- [42] L. V. Boccaccini et al., «Present Status of the Conceptual Design of the EU Test Blanket Systems,» *Fusion Engineering and Design*, vol. 86, pp. 478-483, 2011.
- [43] P. V. Vladimirov et al., «Development and Characterization of Advanced Neutron Multiplier Materials,» *Journal of Nuclear Materials*, vol. 543, n. 152593, 2021.
- [44] G. Zhou, «Design of Two Experimental Mock-Ups as Proof-of-Concept and Validation Test Rigs for the Enhanced EU DEMO HCPB Blanket,» *Fusion Science and Technology*, vol. 75, pp. 1016-1023, 2019.
- [45] C. Bachmann et al., «Overview over DEMO design integration challenges and their impact on component design concepts,» *Fusion Engineering and Design*, vol. 136, pp. 87-95, 2018.
- [46] R. Gaisin, «Progress in manufacture, processing and characterization of NMM - 2022,» EUROfusion IDM Ref. :2Q7T4P, 2022.
- [47] A. Gioè et al., «Exploratory thermo-mechanical assessment of a pin of the Helium Cooled Pebble Bed breeding blanket concept with a modular pin monoblock design for the EU DEMO nuclear fusion reactor,» *Fusion Engineering and Design*, vol. 220, 2025.
- [48] F. A. Hernández et al., «Innovative Breeding Blanket Concepts and Overview of Future R&D Needs for Breeding Blanket Development in EU,» in *Workshop on Advancements in Fusion Breeding Blankets Development*, Hefei, China, November 5-6, 2024.

- [49] I. A. Maione et al., «BB HCPB System Requirements Document (SRD) for the Definition Dossier,» EUROfusion IDM Ref. : 2PAUZ4 v1.5, 2020.
- [50] L. Pareschi et al., «An introduction to Monte Carlo method for the Boltzmann equation,» *ESAIM: Proc.* , vol. 10, pp. 35-75, 2002.
- [51] J. S. Bull et al., MCNP® Code Version 6.3.1 Build Guide, Los Alamos, NM, USA: Los Alamos National Laboratory Tech. Rep. LA-UR-25-22869, 2025.
- [52] G. Zhou, «Nuclear_heat_SP9-npd-D_March2023.xlsx,» file, personal communication, 2023.
- [53] G. A. Spagnuolo, «BB-PHTS Interface Requirements,» EUROfusion IDM Ref. :2NAWQ5, 2018.
- [54] G. Zhou, «BB.HCPB-DEF-2-CD1-PIN Concept Datasheet,» EUROfusion IDM Ref. :2PBXBN, 2020.
- [55] S. L. Dixon and C.A. Hall, Fluid Mechanics and Thermodynamics of Turbomachinery, 7th edn (Waltham: Butterworth-Heinemann).
- [56] K. Kugeler and Z. Zhang, Modular High-temperature Gas-cooled Reactor Power Plant, Berlin: Springer, 2019.
- [57] F. A. Hernández et al., «An enhanced, near-term HCPB design as driver blanket for the EU DEMO,» *Fusion Engineering and Design*, vol. 146, pp. 1186-1191, 2019.
- [58] G. Zhou et al., «Thermal hydraulics activities for consolidating HCPB breeding blanket of the European DEMO,» *Nuclear Fusion*, vol. 60, n. 9, 2020.
- [59] «Final Report on Deliverable Thermo-hydraulic analysis support – 2018,» EUROfusion IDM Ref. : 2P3AQ7, 2019.
- [60] ANSYS Inc., Discovery SpaceClaim, Release 2023 R2, Canonsburg, PA, USA, 2023.
- [61] ANSYS Inc. , ANSYS Meshing User's Guide, Release 2023 R2, Canonsburg, PA, USA, 2023.
- [62] IUPAC Periodic Table of the Elements, 04/05/2022.
- [63] G. Zhou et al. , «HCPB Design and Analysis - Interim Report 2021,» EUROfusion IDM Ref. : 2Q4YMC.
- [64] G. Veres, «Thermo-hydraulic analysis support – 2018,» EUROfusion IDM Ref. :2P3AQ7 v1.0, 2019.

- [65] SimScale, [Online]. Available: <https://www.simscale.com/docs/simulation-setup/global-settings/k-omega-sst/#k-omega-sst>.
- [66] S. Timoshenko et al., Theory of Elasticity, McGraw-Hill Companies, 1970.
- [67] [Online]. Available: <https://www.sciencedirect.com/topics/engineering/elasticity-theory..>
- [68] O. Zienkiewicz et al., The Finite Element Method: Its Basis and Fundamentals, Butterworth-Heinemann, 2005.
- [69] C. Bachmann et al., «Initial definition of structural load conditions in DEMO,» *Fusion Engineering and Design*, vol. 9131, 2017.
- [70] American Society of Mechanical Engineers, Asme boiler and pressure vessel code, New York, USA.
- [71] IAEA Safety Standards Series No. SSG-2, Deterministic, Safety Analysis for Nuclear Power Plants, 2010.
- [72] ITER Organization, Structural design criteria for in-vessel components.
- [73] W. -S. Choi et al., *Nuclear Engineering and Design*, vol. 241, n. 8, pp. 2653-2659, 2011.
- [74] F. A. Hernández, «Personal communication, 27/02/2024».
- [75] Materials Properties Handbook - EUROFER97, <https://idm.eurofusion.org/?uid:2NZHBS>.
- [76] «Python documentation,» [Online]. Available: <https://docs.python.org/3/>.
- [77] G. A. Spagnuolo et al., «Development of load specifications for the design of the breeding blanket system,» *Fusion Engineering and Design*, vol. 157, 2020.

Appendix A

The following tables are relevant to analyses performed adopting the Ansys Fluent Standard Roughness model. The values out of range of the domain's working conditions are highlighted in red.

Table A 1

Case 1 layout: Effect of roughness on surfaces "a"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.249	697	526	470	494	494	391	853	810	475
0.30	220	40.00	0.249	697	525	470	492	494	390	851	809	475
0.30	220	60.00	0.250	696	525	470	492	495	390	849	807	475
0.30	220	80.00	0.251	696	525	470	492	495	391	848	807	475
0.30	220	100.00	0.252	695	524	470	492	496	391	848	806	475

Table A 2

Case 1 layout: Effect of roughness on surfaces "a" and "b"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.249	697	526	470	494	494	391	853	810	475
0.30	220	40.00	0.249	689	525	470	492	494	390	847	803	474
0.30	220	60.00	0.251	686	524	470	492	495	390	845	800	475
0.30	220	80.00	0.253	685	523	470	491	496	391	844	799	475
0.30	220	100.00	0.254	686	523	471	491	497	391	845	800	475

Table A 3

Case 1 layout: Effect of coolant ΔT												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.249	697	526	470	494	494	391	853	810	475
0.30	240	20.00	0.213	725	544	482	509	510	400	875	835	486
0.30	260	20.00	0.185	753	563	494	525	526	411	898	861	498
0.30	280	20.00	0.162	781	582	506	542	543	422	922	887	510
0.30	300	20.00	0.144	809	600	518	558	559	433	945	913	522

Table A 4

Case 1 layout: Effect of HF_0												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.249	697	526	470	494	494	391	853	810	475
0.48	220	20.00	0.295	687	527	521	496	494	401	848	802	529
0.65	220	20.00	0.345	679	527	570	500	495	409	844	796	581
0.83	220	20.00	0.398	672	528	617	503	496	417	841	791	630
1.00	220	20.00	0.456	666	529	656	505	497	424	838	786	672

Table A 5

Case 1 layout: Effect of coolant ΔT under $HF_0 = 1\text{MW/m}^2$ conditions												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
1.00	220	20.00	0.456	666	529	656	505	497	424	838	786	672
1.00	240	20.00	0.388	693	548	673	522	514	436	860	811	688
1.00	260	20.00	0.334	720	568	689	540	531	449	883	836	704
1.00	280	20.00	0.292	747	588	704	557	548	462	905	861	719
1.00	300	20.00	0.258	774	607	720	575	565	475	928	886	735

Table A 6

Case 2 layout: Effect of roughness on surfaces "a"												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.414	609	518	466	486	518	386	814	746	470
0.30	220	40.00	0.426	607	518	466	486	518	386	812	744	470
0.30	220	60.00	0.441	606	518	466	486	519	386	810	742	470
0.30	220	80.00	0.455	604	518	466	486	519	386	809	741	470
0.30	220	100.00	0.469	603	518	466	486	519	386	808	740	470

Table A 7

Case 2 layout: Effect of roughness on surfaces "a" and "b"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.414	609	518	466	486	518	386	814	746	470
0.30	220	40.00	0.438	609	518	466	486	518	386	813	746	470
0.30	220	60.00	0.472	602	518	466	486	519	386	809	739	470
0.30	220	80.00	0.503	594	518	466	486	519	386	805	734	470
0.30	220	100.00	0.533	588	518	466	486	519	386	802	729	470

Table A 8

Case 2 layout: Effect of coolant ΔT												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.414	609	518	466	486	518	386	814	746	470
0.30	240	20.00	0.359	632	536	477	501	537	395	833	767	481
0.30	260	20.00	0.316	656	555	488	517	557	404	853	789	492
0.30	280	20.00	0.281	679	574	499	533	577	414	873	811	503
0.30	300	20.00	0.253	702	593	510	549	597	424	893	833	514

Table A 9

Case 2 layout: Effect of HF ₀												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.414	609	518	466	486	518	386	814	746	470
0.48	220	20.00	0.486	606	518	517	489	517	396	813	744	524
0.65	220	20.00	0.564	603	519	567	493	517	405	812	742	577
0.83	220	20.00	0.648	600	520	613	496	517	413	811	740	627
1.00	220	20.00	0.738	597	520	653	498	517	420	811	738	669

Table A 10

Case 2 layout: Effect of coolant ΔT under $HF_0 = 1\text{MW/m}^2$ conditions												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
1.00	220	20.00	0.738	597	520	653	498	517	420	811	738	669
1.00	240	20.00	0.637	620	540	670	515	537	432	830	759	685
1.00	260	20.00	0.558	642	559	685	532	557	444	849	780	700
1.00	280	20.00	0.495	662	578	700	549	576	456	868	799	715
1.00	300	20.00	0.443	684	598	716	566	596	468	887	820	730

Table A 11

Case 3 layout: Effect of roughness on surfaces "a"												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	20.00	0.565	623	520	470	489	512	390	838	782	474
0.30	220	40.00	0.598	620	520	470	488	512	390	835	779	474
0.30	220	60.00	0.630	618	520	470	488	513	390	832	777	474
0.30	220	80.00	0.661	616	520	470	488	514	390	831	775	474
0.30	220	100.00	0.691	614	520	470	488	514	390	829	773	474

Table A 12

Case 3 layout: Effect of roughness on surfaces "a" and "b"												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	20.00	0.565	623	520	470	489	512	390	838	782	474
0.30	220	40.00	0.635	617	520	470	488	513	390	834	777	474
0.30	220	60.00	0.707	606	520	470	488	514	390	828	769	474
0.30	220	80.00	0.778	597	520	470	488	514	390	823	762	474
0.30	220	100.00	0.849	590	519	470	487	515	390	820	757	474

Table A 13

Case 3 layout: Effect of coolant ΔT												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.565	623	520	470	489	512	390	838	782	474
0.30	240	20.00	0.492	646	539	482	504	530	400	857	803	486
0.30	260	20.00	0.435	669	558	494	520	549	410	877	825	498
0.30	280	20.00	0.389	691	577	506	537	568	421	897	846	510
0.30	300	20.00	0.351	715	597	518	554	587	432	919	868	522

Table A 14

Case 3 layout: Effect of HF_0												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.565	623	520	470	489	512	390	838	782	474
0.48	220	20.00	0.663	618	520	521	492	511	400	835	778	528
0.65	220	20.00	0.770	614	521	570	495	511	409	834	776	580
0.83	220	20.00	0.886	611	522	616	498	512	417	833	773	629
1.00	220	20.00	1.01	608	522	655	500	512	423	831	771	671

Table A 15

Case 3 layout: Effect of coolant ΔT under $HF_0 = 1\text{MW/m}^2$ conditions												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
1.00	220	20.00	1.01	608	522	655	500	512	423	831	771	671
1.00	240	20.00	0.872	630	542	672	518	531	436	851	793	687
1.00	260	20.00	0.765	653	561	688	535	550	448	871	814	703
1.00	280	20.00	0.680	676	581	704	552	569	461	892	836	718
1.00	300	20.00	0.611	699	601	720	570	588	475	912	857	734

Table A 16

Case 7 layout: Effect of roughness on surfaces "a"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.258	677	522	468	490	509	388	843	795	472
0.30	220	40.00	0.259	676	522	468	489	509	388	841	794	472
0.30	220	60.00	0.260	675	522	468	489	509	388	839	792	472
0.30	220	80.00	0.262	674	521	468	489	510	388	838	791	472
0.30	220	100.00	0.264	673	521	468	489	510	388	837	790	472

Table A 17

Case 7 layout: Effect of roughness on surfaces "a" and "b"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.258	677	522	468	490	509	388	843	795	472
0.30	220	40.00	0.260	667	522	468	489	509	388	837	787	472
0.30	220	60.00	0.262	667	521	468	489	510	388	836	786	472
0.30	220	80.00	0.266	666	520	468	488	510	388	835	786	472
0.30	220	100.00	0.269	661	520	468	488	511	388	833	782	472

Table A 18

Case 7 layout: Effect of coolant ΔT												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.258	677	522	468	490	509	388	843	795	472
0.30	240	20.00	0.221	703	540	479	505	526	397	864	819	483
0.30	260	20.00	0.192	730	559	490	521	545	407	887	843	494
0.30	280	20.00	0.169	756	577	502	537	563	417	909	868	506
0.30	300	20.00	0.150	783	596	513	553	582	428	931	893	517

Table A 19

Case 7 layout: Effect of HF_0												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.258	677	522	468	490	509	388	843	795	472
0.48	220	20.00	0.305	668	523	519	493	508	398	839	788	526
0.65	220	20.00	0.357	661	524	568	496	509	407	836	783	578
0.83	220	20.00	0.412	654	525	614	499	509	415	833	778	628
1.00	220	20.00	0.472	649	525	654	502	509	421	831	774	670

Table A 20

Case 7 layout: Effect of coolant ΔT under $HF_0 = 1\text{MW/m}^2$ conditions												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
1.00	220	20.00	0.472	649	525	654	502	509	421	831	774	670
1.00	240	20.00	0.401	675	545	671	519	528	434	852	798	686
1.00	260	20.00	0.346	701	564	687	536	547	446	874	822	702
1.00	280	20.00	0.303	727	583	702	553	565	458	895	846	716
1.00	300	20.00	0.268	752	602	717	570	584	471	917	870	732

Table A 21

Case 6 layout: Effect of roughness on surfaces "a"												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.289	648	519	467	487	514	386	830	774	471
0.30	220	40.00	0.292	647	519	467	487	515	386	829	773	471
0.30	220	60.00	0.296	645	519	467	486	515	386	827	771	471
0.30	220	80.00	0.300	644	519	467	486	515	386	825	769	471
0.30	220	100.00	0.303	643	518	467	486	515	387	824	768	471

Table A 22

Case 6 layout: Effect of roughness on surfaces "a" and "b"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.289	648	519	467	487	514	386	830	774	471
0.30	220	40.00	0.294	643	519	467	487	515	386	827	769	471
0.30	220	60.00	0.303	640	518	467	486	515	386	825	767	471
0.30	220	80.00	0.311	634	518	467	486	516	386	821	762	471
0.30	220	100.00	0.318	628	518	467	486	516	387	818	758	471

Table A 23

Case 6 layout: Effect of coolant ΔT												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.289	648	519	467	487	514	386	830	774	471
0.30	240	20.00	0.248	674	537	478	502	533	396	851	797	482
0.30	260	20.00	0.216	699	556	489	518	553	405	872	821	493
0.30	280	20.00	0.191	724	575	500	534	572	415	893	844	504
0.30	300	20.00	0.170	749	593	511	551	592	425	914	867	515

Table A 24

Case 6 layout: Effect of HF ₀												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.289	648	519	467	487	514	386	830	774	471
0.48	220	20.00	0.341	641	520	518	491	514	397	828	769	525
0.65	220	20.00	0.398	635	521	567	494	514	406	825	765	577
0.83	220	20.00	0.460	630	522	614	497	515	414	823	761	627
1.00	220	20.00	0.525	625	522	654	500	515	421	821	757	670

Table A 25

Case 6 layout: Effect of coolant ΔT under $HF_0 = 1\text{MW/m}^2$ conditions												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
1.00	220	20.00	0.525	625	522	654	500	515	421	821	757	670
1.00	240	20.00	0.449	650	542	670	517	534	432	842	780	686
1.00	260	20.00	0.389	675	561	686	534	554	445	863	803	701
1.00	280	20.00	0.341	699	580	701	551	573	457	883	826	715
1.00	300	20.00	0.302	724	599	716	568	592	469	904	849	731

Table A 26

Case 8 layout: Effect of roughness on surfaces "a"												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	20.00	1.77	570	517	466	484	519	385	791	706	470
0.30	220	40.00	1.99	567	517	466	485	519	385	790	705	470
0.30	220	60.00	2.19	564	517	466	485	519	385	788	703	470
0.30	220	80.00	2.37	563	517	466	485	520	385	787	702	470
0.30	220	100.00	2.49	562	517	466	485	520	385	786	702	470

Table A 27

Case 8 layout: Effect of roughness on surfaces "a" and "b"												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m^2	$^{\circ}\text{C}$	μm	Bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	20.00	1.77	570	517	466	484	519	385	791	706	470
0.30	220	40.00	2.23	565	517	466	485	520	385	787	700	470
0.30	220	60.00	2.66	560	518	466	485	520	385	783	695	470
0.30	220	80.00	3.04	559	518	466	485	520	385	781	691	470
0.30	220	100.00	3.31	557	518	466	485	520	386	780	690	470

Table A 28

Case 8 layout: Effect of coolant ΔT												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	1.77	570	517	466	484	519	385	791	706	470
0.30	240	20.00	1.54	590	536	477	500	539	394	809	726	481
0.30	260	20.00	1.36	610	555	488	517	559	403	827	745	491
0.30	280	20.00	1.21	629	575	499	533	579	413	845	764	502
0.30	300	20.00	1.10	648	594	510	549	600	423	863	783	513

Table A 29

Case 8 layout: Effect of HF_0												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	1.77	570	517	466	484	519	385	791	706	470
0.48	220	20.00	2.10	570	517	517	488	519	396	792	706	524
0.65	220	20.00	2.45	569	518	566	491	518	405	792	706	576
0.83	220	20.00	2.83	569	518	613	494	518	413	792	705	626
1.00	220	20.00	3.24	568	519	653	497	518	420	792	704	669

Table A 30

Case 8 layout: Effect of coolant ΔT under $HF_0 = 1 \text{ MW/m}^2$ conditions												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
1.00	220	20.00	3.24	568	519	653	497	518	420	792	704	669
1.00	240	20.00	2.80	589	538	669	514	538	431	811	724	685
1.00	260	20.00	2.46	609	558	685	531	558	443	829	744	700
1.00	280	20.00	2.18	629	577	700	548	578	455	847	763	715
1.00	300	20.00	1.96	648	597	715	565	598	468	865	782	730

Appendix B

The following tables are relevant to analyses performed adopting the Ansys Fluent High Roughness (Icing) model. The values out of range of the domain's working conditions are highlighted in red.

Table B 1

Case 2 layout: Effect of roughness on surfaces "a", "b" and "c"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.459	603	518	464	486	519	386	810	741	468
0.30	220	40.00	0.486	593	518	464	486	519	386	804	733	468
0.30	220	60.00	0.507	587	518	464	486	519	386	799	727	468
0.30	220	80.00	0.526	582	518	464	485	519	386	796	723	468
0.30	220	100.00	0.544	578	518	464	485	519	386	793	720	468

Table B 2

Case 2 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.544	578	518	456	485	519	386	793	720	460
0.30	240	100	0.469	601	536	467	500	538	395	811	740	470
0.30	260	100	0.411	624	555	478	517	558	405	831	761	481
0.30	280	100	0.364	647	574	489	533	578	415	850	782	492
0.30	300	100	0.326	669	593	500	549	598	425	869	803	503

Table B 3

Case 2 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.618	565	518	453	485	519	387	784	709	456
0.30	240	200	0.534	588	536	464	500	539	396	802	729	467
0.30	260	200	0.467	610	556	475	517	559	405	821	749	478
0.30	280	200	0.414	632	575	486	533	579	415	839	770	488
0.30	300	200	0.370	655	594	496	549	599	426	858	790	499

Table B 4

Case 2 layout: Effect of HF ₀ with roughness(a,b,c) = 100 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.544	578	518	456	485	519	386	793	720	460
0.48	220	100	0.641	574	518	502	488	518	397	792	717	509
0.65	220	100	0.747	571	519	547	492	518	406	792	716	556
0.83	220	100	0.861	569	519	589	494	518	414	791	714	602
1.00	220	100	0.983	566	519	629	497	518	420	791	713	644

Table B 5

Case 2 layout: Effect of HF ₀ with roughness(a,b,c) = 200 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.618	565	518	453	485	519	387	784	709	456
0.48	220	200	0.729	562	518	496	488	518	397	783	707	502
0.65	220	200	0.849	560	518	539	491	518	406	784	706	548
0.83	220	200	0.978	558	519	580	494	518	414	784	705	592
1.00	220	200	1.12	556	519	618	497	518	421	784	704	634

Table B 6

Case 4 layout: Effect of roughness on surfaces "a", "b" and "c"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.465	605	518	516	485	519	385	839	747	520
0.30	220	40.00	0.493	595	518	516	485	519	386	832	738	520
0.30	220	60.00	0.515	589	517	516	484	519	385	827	733	520
0.30	220	80.00	0.535	583	517	516	484	519	386	824	728	520
0.30	220	100.00	0.554	579	517	516	484	519	386	821	725	520

Table B 7

Case 4 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.556	579	517	497	484	519	386	821	725	501
0.30	240	100	0.480	602	536	509	499	538	395	840	746	513
0.30	260	100	0.421	625	555	520	516	558	405	859	767	524
0.30	280	100	0.373	648	574	532	532	578	415	878	788	536
0.30	300	100	0.335	671	593	543	548	598	425	898	809	547

Table B 8

Case 4 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.635	566	518	487	484	520	386	812	714	491
0.30	240	200	0.549	588	536	498	499	539	395	830	734	502
0.30	260	200	0.481	611	555	509	516	559	405	849	754	513
0.30	280	200	0.427	633	574	520	532	579	415	868	775	524
0.30	300	200	0.382	655	593	531	548	599	426	887	796	535

Table B 9

Case 4 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.556	579	517	497	484	519	386	821	725	501
0.48	220	100	0.652	575	517	561	487	518	396	820	722	567
0.65	220	100	0.757	572	518	620	491	518	405	819	721	629
0.83	220	100	0.868	570	519	671	496	518	413	819	719	682
1.00	220	100	0.988	568	519	713	512	518	420	819	718	726

Table B 10

Case 4 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{in-sheet}$	$T_{max}^{out-sheet}$	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.635	565	517	487	484	519	386	811	713	491
0.48	220	200	0.745	562	517	547	487	519	396	811	711	553
0.65	220	200	0.865	560	518	604	490	518	405	811	710	613
0.83	220	200	0.992	558	519	654	493	518	413	811	709	665
1.00	220	200	1.13	557	519	695	502	518	420	811	709	708

Table B 11

Case 5 layout: Effect of roughness on surfaces "a", "b" and "c"										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.340	577	514	537	523	1005	841	541
0.30	220	40.00	0.366	563	514	534	523	998	831	538
0.30	220	60.00	0.387	553	514	533	523	993	824	537
0.30	220	80.00	0.405	549	514	532	523	990	819	536
0.30	220	100.00	0.421	547	514	532	523	987	814	535

Table B 12

Case 5 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm										
HF ₀	ΔT	rough.	Δp_{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.421	547	514	532	523	987	814	535
0.30	240	100	0.360	566	533	550	544	1004	832	553
0.30	260	100	0.311	585	551	568	564	1021	850	571
0.30	280	100	0.272	605	570	586	585	1038	868	590
0.30	300	100	0.241	624	588	605	606	1055	886	608

Table B 13

Case 5 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.479	539	515	530	523	978	801	533
0.30	240	200	0.409	558	533	548	544	994	818	551
0.30	260	200	0.354	577	552	566	565	1011	835	569
0.30	280	200	0.310	596	571	585	585	1027	852	588
0.30	300	200	0.274	615	589	603	606	1043	869	606

Table B 14

Case 5 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.421	547	514	532	523	987	814	535
0.48	220	100	0.485	542	515	569	523	983	806	576
0.65	220	100	0.554	539	515	606	523	979	798	616
0.83	220	100	0.626	536	515	640	523	976	792	652
1.00	220	100	0.703	535	515	669	526	974	786	685

Table B 15

Case 5 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.479	539	515	530	523	978	801	533
0.48	220	200	0.551	535	515	564	523	975	793	571
0.65	220	200	0.628	532	515	600	523	972	786	610
0.83	220	200	0.711	530	515	633	523	969	780	645
1.00	220	200	0.798	530	515	662	523	967	775	677

Table B 16

Case 9 layout: Effect of roughness on surfaces "a", "b" and "c"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.335	602	518	474	485	517	387	810	740	478
0.30	220	40.00	0.362	594	518	474	486	517	387	804	733	478
0.30	220	60.00	0.383	587	518	474	485	517	387	799	728	478
0.30	220	80.00	0.402	583	518	474	485	518	387	796	724	478
0.30	220	100.00	0.420	579	518	474	485	518	387	793	720	478

Table B 17

Case 9 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.420	579	518	465	485	518	387	794	720	468
0.30	240	100	0.365	602	537	476	501	537	397	813	742	480
0.30	260	100	0.321	625	556	488	517	557	406	832	763	491
0.30	280	100	0.287	649	575	499	534	578	417	852	784	503
0.30	300	100	0.258	671	594	511	551	597	427	871	805	514

Table B 18

Case 9 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.495	566	518	460	485	519	388	785	710	464
0.30	240	200	0.429	589	537	472	501	538	397	803	730	475
0.30	260	200	0.378	612	556	483	518	558	407	822	751	487
0.30	280	200	0.336	634	576	495	534	578	417	841	772	498
0.30	300	200	0.302	657	595	506	551	599	428	861	793	510

Table B 19

Case 9 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.420	579	518	465	485	518	387	794	720	468
0.48	220	100	0.496	575	518	514	489	518	398	793	718	520
0.65	220	100	0.578	572	519	561	492	518	407	793	717	571
0.83	220	100	0.665	569	519	606	495	517	414	792	715	619
1.00	220	100	0.759	567	520	645	497	518	421	792	714	661

Table B 20

Case 9 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.495	566	518	460	485	519	388	785	710	464
0.48	220	200	0.584	563	519	507	489	518	398	785	708	513
0.65	220	200	0.680	561	519	552	492	518	407	785	707	562
0.83	220	200	0.783	559	519	595	495	518	415	785	706	608
1.00	220	200	0.893	557	520	634	497	518	421	785	705	650

Table B 21

Case 11 layout: Effect of roughness on surfaces "a", "b" and "c"												
HF_0	ΔT	rough.	Δp_{tot}	T_{max} cladding	T_{max} plug	T_{max} cap	T_{max} shell	T_{max} in-sheet	T_{max} out-sheet	T_{max} breeder	T_{max} multiplier	T_{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.289	605	519	485	488	516	389	813	743	490
0.30	220	40.00	0.315	596	519	485	487	516	389	806	735	490
0.30	220	60.00	0.336	590	519	485	487	517	389	802	730	490
0.30	220	80.00	0.355	585	519	486	487	517	389	798	726	490
0.30	220	100.00	0.373	581	519	486	487	517	389	795	722	490

Table B 22

Case 11 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.373	581	519	474	487	517	389	795	722	478
0.30	240	100	0.325	604	537	486	503	536	399	815	744	490
0.30	260	100	0.287	628	557	499	519	556	409	834	765	503
0.30	280	100	0.257	651	576	511	536	575	420	854	786	515
0.30	300	100	0.232	674	595	524	553	595	431	874	808	527

Table B 23

Case 11 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.448	568	519	469	487	518	390	786	711	473
0.30	240	200	0.390	591	537	481	502	537	399	805	732	485
0.30	260	200	0.344	614	557	493	519	557	410	824	753	497
0.30	280	200	0.307	636	576	506	536	577	420	844	774	509
0.30	300	200	0.276	659	596	518	553	596	431	863	795	521

Table B 24

Case 11 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.373	581	519	474	487	517	389	795	722	478
0.48	220	100	0.440	577	519	527	490	516	399	794	720	534
0.65	220	100	0.513	573	519	577	493	516	408	794	718	587
0.83	220	100	0.591	571	520	624	496	516	416	794	716	637
1.00	220	100	0.674	568	520	663	498	516	423	793	715	679

Table B 25

Case 11 layout: Effect of HF ₀ with roughness(a,b,c) = 200 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.448	568	519	469	487	518	390	786	711	473
0.48	220	200	0.528	564	519	519	490	517	400	786	709	525
0.65	220	200	0.615	562	519	567	493	517	409	786	708	576
0.83	220	200	0.708	560	520	612	496	517	416	786	707	625
1.00	220	200	0.808	558	520	651	498	517	423	786	706	667

Table B 26

Case 12 layout: Effect of roughness on surfaces "a", "b" and "c"												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.269	609	520	501	490	511	394	816	747	505
0.30	220	40.00	0.295	600	520	501	490	512	394	810	739	506
0.30	220	60.00	0.316	594	520	501	490	513	394	806	734	506
0.30	220	80.00	0.336	589	520	501	490	513	394	802	730	506
0.30	220	100.00	0.354	585	520	501	490	513	394	799	726	506

Table B 27

Case 12 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm												
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} in-sheet	T _{max} out-sheet	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.354	585	520	488	490	513	394	799	726	492
0.30	240	100	0.309	610	539	502	506	533	405	820	749	506
0.30	260	100	0.274	634	559	516	523	552	416	840	771	520
0.30	280	100	0.245	658	579	530	541	571	428	861	794	534
0.30	300	100	0.222	682	598	544	558	591	440	882	816	548

Table B 28

Case 12 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.428	572	520	482	489	515	395	791	715	486
0.30	240	200	0.374	596	539	495	506	534	406	810	737	499
0.30	260	200	0.330	620	559	509	523	554	417	830	759	513
0.30	280	200	0.295	644	579	523	541	573	429	851	781	527
0.30	300	200	0.266	667	599	537	558	593	441	871	803	541

Table B 29

Case 12 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.354	585	520	488	490	513	394	799	726	492
0.48	220	100	0.417	581	520	544	493	513	404	799	724	551
0.65	220	100	0.486	578	521	598	496	514	413	798	722	608
0.83	220	100	0.559	575	521	646	499	514	420	797	720	659
1.00	220	100	0.638	572	521	685	501	514	427	797	719	701

Table B 30

Case 12 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm												
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max in-sheet}}$	$T_{\text{max out-sheet}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.428	572	520	482	489	515	395	791	715	486
0.48	220	200	0.505	569	520	535	493	515	405	790	714	542
0.65	220	200	0.589	566	521	586	496	515	413	790	712	596
0.83	220	200	0.678	564	521	633	498	515	421	790	711	646
1.00	220	200	0.772	562	521	672	501	515	427	790	710	687

Table B 31

Case 10 layout: Effect of roughness on surfaces "a", "b" and "c"										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.937	552	514	530	523	1001	837	534
0.30	220	40.00	1.03	547	514	528	523	995	827	532
0.30	220	60.00	1.10	544	514	527	523	991	821	531
0.30	220	80.00	1.16	541	515	527	523	988	816	530
0.30	220	100.00	1.21	539	515	526	523	985	813	529

Table B 32

Case 10 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	1.21	539	515	526	523	985	813	529
0.30	240	100	1.03	558	533	544	544	1001	829	547
0.30	260	100	0.893	577	551	562	564	1017	845	565
0.30	280	100	0.780	595	570	581	585	1034	862	584
0.30	300	100	0.689	615	588	599	606	1050	878	602

Table B 33

Case 10 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	1.39	533	515	525	523	978	802	528
0.30	240	200	1.19	551	534	543	544	993	818	546
0.30	260	200	1.02	571	552	561	564	1009	833	564
0.30	280	200	0.896	591	571	580	585	1025	849	582
0.30	300	200	0.791	612	589	598	606	1040	864	601

Table B 34

Case 10 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	100	1.21	539	515	526	523	985	813	529
0.48	220	100	1.40	536	515	560	523	982	806	567
0.65	220	100	1.59	535	515	595	523	979	800	605
0.83	220	100	1.80	535	515	628	523	977	794	642
1.00	220	100	2.02	534	515	657	523	975	789	673

Table B 35

Case 10 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	200	1.39	533	515	525	523	978	802	528
0.48	220	200	1.60	530	515	556	523	975	796	563
0.65	220	200	1.83	530	515	590	523	973	790	600
0.83	220	200	2.07	530	516	622	523	970	785	635
1.00	220	200	2.33	530	516	651	523	969	781	667

Table B 36

Case 13 layout: Effect of roughness on surfaces "a", "b" and "c"										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	20.00	0.152	616	514	545	523	1015	855	550
0.30	220	40.00	0.164	598	514	543	523	1007	841	547
0.30	220	60.00	0.174	583	514	541	523	1000	831	545
0.30	220	80.00	0.185	571	513	540	523	995	822	544
0.30	220	100.00	0.194	561	513	539	523	991	815	543

Table B 37

Case 13 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.194	561	513	539	523	991	815	543
0.30	240	100	0.165	584	532	558	544	1009	835	562
0.30	260	100	0.142	607	550	576	565	1028	856	580
0.30	280	100	0.124	629	568	595	585	1046	876	598
0.30	300	100	0.109	651	586	613	606	1064	896	616

Table B 38

Case 13 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.229	542	514	535	524	977	794	539
0.30	240	200	0.195	562	532	554	544	995	812	558
0.30	260	200	0.168	581	550	573	565	1012	831	576
0.30	280	200	0.147	602	568	592	586	1030	850	595
0.30	300	200	0.129	623	586	611	606	1048	869	614

Table B 39

Case 13 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.194	561	513	539	523	991	815	543
0.48	220	100	0.224	548	514	579	523	986	804	586
0.65	220	100	0.257	543	514	619	523	981	795	628
0.83	220	100	0.291	542	514	653	524	977	787	665
1.00	220	100	0.328	541	514	683	532	974	780	698

Table B 40

Case 13 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	200	0.229	542	514	535	524	977	794	539
0.48	220	200	0.264	538	514	572	524	973	785	579
0.65	220	200	0.302	534	515	609	523	969	777	618
0.83	220	200	0.342	533	515	642	523	966	770	654
1.00	220	200	0.384	533	515	671	528	963	763	686

Table B 41

Case 14 layout: Effect of roughness on surfaces "a", "b" and "c"										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	20.00	0.0809	648	514	552	523	1021	865	557
0.30	220	40.00	0.0858	630	514	549	523	1012	851	553
0.30	220	60.00	0.0902	617	514	547	523	1006	841	551
0.30	220	80.00	0.0946	606	513	546	523	1001	833	550
0.30	220	100.00	0.0988	595	513	545	523	997	825	549

Table B 42

Case 14 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	100	0.0988	595	513	545	523	997	825	549
0.30	240	100	0.0843	621	532	563	544	1016	847	567
0.30	260	100	0.0729	645	550	582	565	1035	869	586
0.30	280	100	0.0639	668	568	601	585	1054	891	604
0.30	300	100	0.0566	692	586	619	606	1074	913	622

Table B 43

Case 14 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.116	561	514	541	524	981	800	544
0.30	240	200	0.0989	584	532	560	544	999	820	563
0.30	260	200	0.0855	607	550	578	565	1018	841	582
0.30	280	200	0.0747	630	568	597	586	1036	861	601
0.30	300	200	0.0660	652	586	616	606	1055	882	619

Table B 44

Case 14 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	100	0.0988	595	513	545	523	997	825	549
0.48	220	100	0.114	578	514	588	523	990	813	595
0.65	220	100	0.131	563	514	629	523	985	802	639
0.83	220	100	0.148	550	514	665	528	980	793	677
1.00	220	100	0.167	542	514	696	536	976	784	710

Table B 45

Case 14 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{\text{max cladding}}$	$T_{\text{max plug}}$	$T_{\text{max cap}}$	$T_{\text{max shell}}$	$T_{\text{max breeder}}$	$T_{\text{max multiplier}}$	$T_{\text{max tungsten}}$
MW/m^2	$^{\circ}\text{C}$	μm	bar	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$	$^{\circ}\text{C}$
0.30	220	200	0.116	561	514	541	524	981	800	544
0.48	220	200	0.134	546	514	580	524	976	789	587
0.65	220	200	0.154	540	514	619	524	971	779	628
0.83	220	200	0.174	535	514	653	524	967	771	665
1.00	220	200	0.196	533	515	683	531	964	764	697

Table B 46

Case 15 layout: Effect of roughness on surfaces "a", "b" and "c"										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	20.00	0.246	615	518	540	523	1048	862	544
0.30	220	40.00	0.267	596	518	538	523	1040	848	542
0.30	220	60.00	0.288	581	518	536	523	1034	837	540
0.30	220	80.00	0.307	569	518	535	523	1029	829	539
0.30	220	100.00	0.327	559	518	534	523	1025	822	538

Table B 47

Case 15 layout: Effect of coolant ΔT with roughness(a,b,c) = 100 μm										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	100	0.327	559	518	534	523	1025	822	538
0.30	240	100	0.280	582	538	552	544	1042	841	556
0.30	260	100	0.243	605	557	570	564	1060	861	574
0.30	280	100	0.214	627	576	588	585	1077	881	591
0.30	300	100	0.191	648	596	605	606	1094	900	609

Table B 48

Case 15 layout: Effect of coolant ΔT with roughness(a,b,c) = 200 μm										
HF ₀	ΔT	rough.	Δp _{tot}	T _{max} cladding	T _{max} plug	T _{max} cap	T _{max} shell	T _{max} breeder	T _{max} multiplier	T _{max} tungsten
MW/m ²	°C	μm	bar	°C	°C	°C	°C	°C	°C	°C
0.30	220	200	0.412	539	519	530	523	1013	801	534
0.30	240	200	0.353	559	539	548	544	1029	819	552
0.30	260	200	0.306	578	558	566	565	1045	837	570
0.30	280	200	0.268	597	578	585	586	1062	855	588
0.30	300	200	0.238	616	597	603	606	1078	873	606

Table B 49

Case 15 layout: Effect of HF_0 with roughness(a,b,c) = 100 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	100	0.327	559	518	534	523	1025	822	538
0.48	220	100	0.376	545	518	574	523	1021	812	581
0.65	220	100	0.429	541	518	613	523	1017	803	623
0.83	220	100	0.485	537	518	648	523	1014	796	660
1.00	220	100	0.545	534	518	679	523	1012	789	693

Table B 50

Case 15 layout: Effect of HF_0 with roughness(a,b,c) = 200 μm										
HF_0	ΔT	rough.	Δp_{tot}	$T_{max}^{cladding}$	T_{max}^{plug}	T_{max}^{cap}	T_{max}^{shell}	$T_{max}^{breeder}$	$T_{max}^{multiplier}$	$T_{max}^{tungsten}$
MW/m^2	$^{\circ}C$	μm	bar	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$	$^{\circ}C$
0.30	220	200	0.412	539	519	530	523	1013	801	534
0.48	220	200	0.475	535	519	567	523	1009	793	573
0.65	220	200	0.543	531	519	603	523	1007	785	613
0.83	220	200	0.614	528	519	637	523	1004	779	649
1.00	220	200	0.690	525	519	666	523	1002	773	681