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Experimental and numerical investigation on the role of holes arrangement on the heat transfer in impingement/effusion cooling schemes

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Abstract

In the present work, two different impingement/effusion geometries have been investigated, both having staggered hole configuration and an equal number of impingement and effusion holes. The first geometry, which is designed in case of low coolant availability, has impingement hole pitch-to-diameter ratios of 10.5 in both orthogonal directions, a jet-to-target plate spacing of 6.5 hole diameters, with effusion holes inclined of 20° with respect to the target surface. The second geometry, which is designed in case of high coolant availability, has impingement hole pitch-to-diameter ratios of 3.0, a jet-to-target plate spacing of 2.5 diameters and normal effusion holes. For each geometry, two relative arrangements between the impingement and effusion holes have been investigated, as well as various Reynolds numbers for the sparser geometry. The experimental investigation has been performed by applying a transient technique, using narrow band thermochromic liquid crystals (TLCs) for surface temperature measurement. A CFD analysis has also been performed in order to support interpretation of the results. Results show unique heat transfer patterns for every investigated geometry. Weak jet-jet interactions have been recorded for the sparser array geometry, while intense secondary peaks and a complex heat transfer pattern are observed for the denser one, which is also strongly influenced by the presence and position of effusion holes. For both the geometries, effusion holes increase heat transfer with respect to impingement-only, which can be mainly attributed to a reduction in flow recirculation for the sparser geometry and to the suppression of spent coolant flow for the denser one.

Keywords: impingement, effusion, cooling, heat transfer coefficient, liquid crystals, CFD, SAS

1. INTRODUCTION

Gas turbine development is characterized by a continuously increasing turbine inlet temperature, which is beneficial for the efficiency and power output of the engine. The main drawback of this trend is the corresponding enhancement of thermal loads on all engine components exposed to the hot gas flow. To sort out this issue, cooling systems have been developed to keep material temperatures to a level that ensures an adequate lifespan of hardware. In modern gas turbines, different cooling techniques are usually applied simultaneously: the interaction among the various systems can strongly affect the performances of

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Nomenclature

Acronyms

<i>CFD</i>	Computational Fluid Dynamics
<i>HTC</i>	Heat Transfer Coefficient [W/mK]
<i>RANS</i>	Reynolds-Averaged Navier-Stokes
<i>SAS</i>	Scale Adaptive Simulation
<i>TLC</i>	Thermochromic Liquid Crystals

Greek symbols

α	thermal diffusivity [m^2/s]
β	angle respect to plate [deg]
μ	dynamic viscosity [Pa/s]

Latin symbols

\dot{m}	mass flow rate [kg/s]
A	area [m^2]
D	diameter [m]
G	mass flow rate over area ratio [kg/m^2s]
h	convective heat transfer coefficient [W/m^2K]
k	thermal conductivity [W/mK]

N	number of holes [-]
Nu	Nusselt number [-]
Re	Reynolds number [-]
S	thickness [m]
T	temperature [K]
t	time [s]
x	streamwise direction [m]
X	jet-to-jet spacing (x direction) [m]
y	lateral direction [m]
Y	jet-to-jet spacing (y direction) [m]
Z	jet-to-target plate spacing [m]

Subscripts

0	Geometry 2 (impingement only), first row
e	effusion
i	impingement
$init$	initial
j	impingement jet
w	wall

8 the single system. Accordingly, a study of the complete cooling configuration is often required to determine
9 its performance: some interesting applications can be found in previous studies carried out by the authors
10 [1, 2, 3].

11 A highly effective cooling system, widely used in combustor liners and nozzle guide vanes, is the combination
12 of impingement and effusion cooling. In this configuration an array of impinging jets is generated by a
13 perforated baffle which cools down the wall opposite to the hot gas path. The spent coolant then feeds an
14 array of effusion holes through the target wall itself and is evacuated on the hot side, creating a protective
15 film layer. The area averaged heat transfer coefficient (HTC) on the cold side of the target wall can be up to
16 55% higher than the ones obtained with impingement alone [4], and up to 10 times the values of effusion only
17 [5]. Heat transfer enhancement by impingement alone is dependent upon the suppression of spent coolant

18 flow (crossflow), which deflects coolant jets and degrades impingement performance [4, 6, 7]. The interaction
19 between impingement and effusion flow fields can increase heat transfer even without crossflow, mainly thanks
20 to the reduction of flow re-entrainment [8, 9], while a minor role seems to be played by flow acceleration near
21 the effusion holes [8, 10]. The presence of impingement plate itself can also be beneficial for hot gas side
22 protection [11]. The overall pressure drop across the liner is indeed distributed between the two layers with
23 the impingement perforations usually set as metering orifices for prescribing the mass flow. This allows to
24 maintain high effusion holes apertures with a reduced pressure drop thus decreasing the jet penetration and
25 improving film cooling development[12].

26 According to previous discussion, it is evident that impingement/effusion cooling schemes represent a feasible
27 strategy to increase cooling efficiency and save coolant. However, the implementation of such systems needs
28 to be evaluated taking into account some possible detrimental aspects. First of all, cost and weight of double
29 wall impingement/effusion scheme are generally higher than simple effusion systems. Moreover, a combination
30 of parameters which maximizes cold side heat transfer may not correspond to an optimal condition in terms
31 of hot side film effectiveness: the ensemble of all the design parameters needs to be considered in order
32 to retrieve the best performance in terms of liner thermal stresses reduction [13]. As a consequence, each
33 particular system requires a dedicated analysis to be performed.

34 The present work focuses on the measurement of HTC distribution on the cold side of the effusion plate of
35 two distinct impingement/effusion systems with different geometric parameters. The effects of the relative
36 positioning of impingement and effusion arrays, as well as of the impingement jets Reynolds number, are
37 experimentally investigated. The analysis of the heat transfer distribution on the inner side of the effusion
38 plate is supported by CFD simulations: the combination of measured and calculated data enables a complete
39 analysis of thermal and fluid-dynamic phenomena, and thus provides significant information on the unique
40 behaviour of each geometry. A similar approach have been already followed by the authors in the past for
41 the investigation of different cooling configurations based on impinging jets [14, 15, 16].

42 **2. EXPERIMENTAL INVESTIGATION**

43 *2.1. Test rig and measurement techniques*

44 Measurements were performed in the Heat Transfer and Combustion Laboratory of the Department of
45 Industrial Engineering of the University of Florence (DIEF).

46 The test rig (depicted in Figure 1) consists of an open-loop, suction type wind tunnel, and is designed to repli-
47 cate, on an enlarged scale, the thermal and fluid-dynamic phenomena of a combined impingement/effusion
48 cooling system. The vacuum system is composed by four inverter controlled vacuum pumps, with a total
49 maximum capacity of about $2400\text{ m}^3/h$, which pull air at ambient pressure and temperature into the rig
50 inlet section. Since a transient technique is used for heat transfer measurements, air needs to undergo a
51 fast and uniform temperature change. As a consequence, the first component encountered by the air flow is
52 an ad-hoc prepared six stage mesh heater. The number of active stages is defined by the required thermal

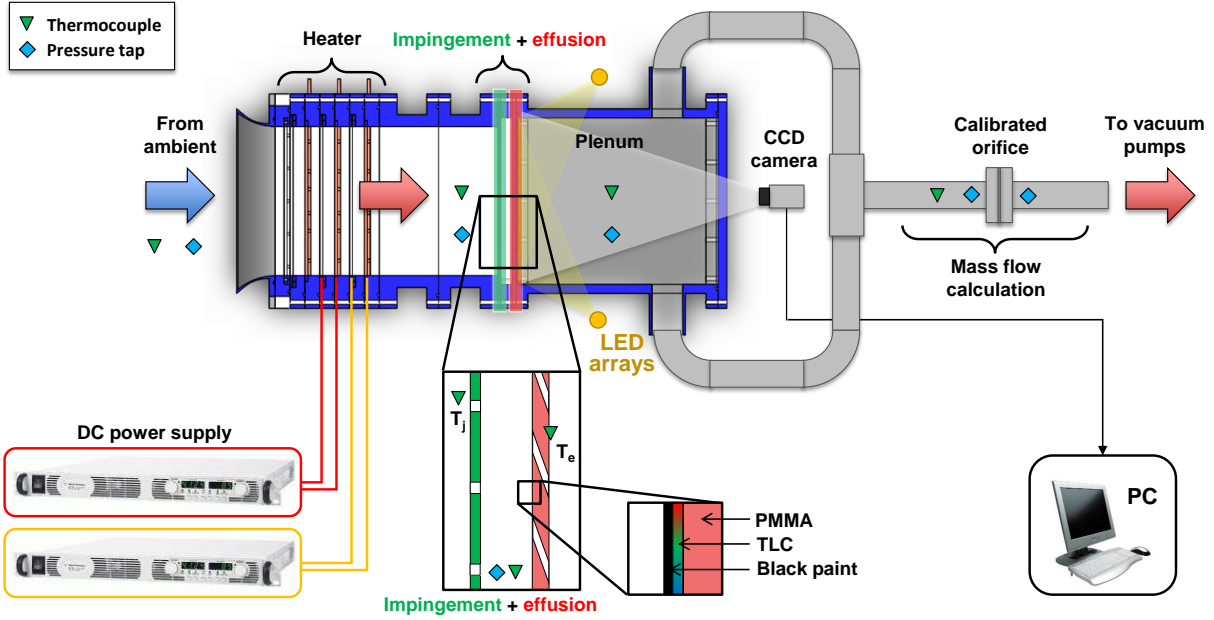


Figure 1: Test rig scheme.

53 power. Electric power is provided to the stages by dedicated DC power supplies. A straight PMMA duct
 54 connects the mesh heater to the cooling geometry model. The small length (200 mm) and the low thermal
 55 conductivity of the material (0.19 W/mK) allow to preserve the uniform temperature profile of the heated
 56 air flow.

57 The cooling geometry is entirely made of transparent PMMA to provide both thermal insulation and optical
 58 access to the inner surfaces. The impingement/effusion system is replicated by two parallel plates, housing
 59 the impingement (I) and effusion (E) holes arrays, and a spacer, built as a square frame, which separates the
 60 two plates and defines the impingement-to-target plate spacing.

61 Two main configurations have been investigated, which will be referred to as Geometry 1 and 2. The corre-
 62 sponding impingement and effusion plates will be indicated as I1 and E1 for the first Geometry, and I2 and
 63 E2 for the second one. A scheme of such geometries is reported in Figure 2. Each configuration presents an
 64 equal number of impingement and effusion holes, arranged in staggered arrays. Geometric features of the two
 65 geometries are summarized in Table 1, namely impingement jet-to-jet spacings in both orthogonal directions
 66 x and y defined on the plate itself (X and Y), jet-to-target plate spacing (Z), effusion hole diameter (D_e),
 67 impingement and effusion plates thickness (S_i and S_e). All values are scaled with respect to the impingement
 68 hole diameter (D_i). The inclination of holes axes with respect to the plate surface (β_i and β_e) is also reported,
 69 as well as the number of impingement and effusion holes N_i and N_e .

70 Differences in geometric features of the two schemes reflect their design targets. The two geometries share
 71 the same values of impingement and effusion plate thickness, as well as the same gap between them. Geom-
 72 etry 1 is designed in case of low coolant availability, thus presenting smaller holes and features aimed at the

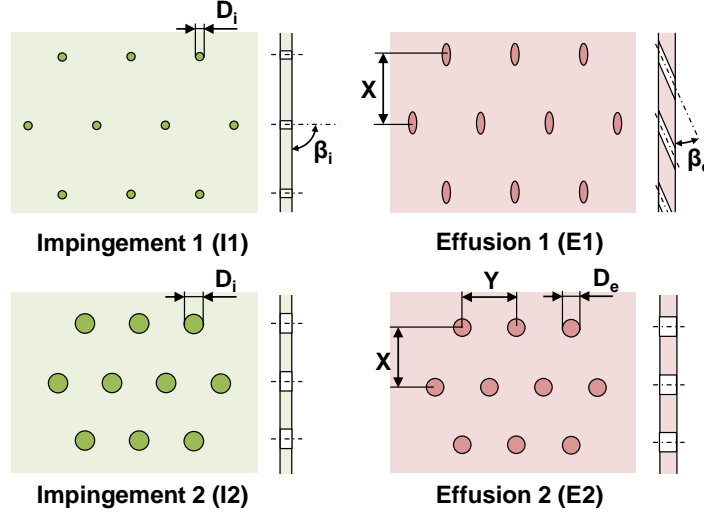


Figure 2: Schemes of Geometry 1 and 2 impingement and effusion plates.

73 maximization of heat pickup (e.g. inclined effusion holes). On the other hand, Geometry 2 is more suitable
 74 when a higher amount of coolant flow is available, and thus houses bigger diameter holes. The analysis of
 75 such geometries allows to determine whether variations of corresponding geometric or fluid dynamic features
 76 have similar effects for very different systems, which is the main goal of this work.

77 Heat transfer performance of an impingement/effusion cooling system also depends upon the relative position
 78 of the impingement and effusion holes ([8, 10, 17]). To investigate such dependency, two different relative
 79 arrangements were examined for both Geometry 1 and 2. In the first arrangement, which will be referred
 80 to as Assembly 1 (A1), the projections of the impingement holes on the effusion plate lie midway between
 81 the effusion holes in both orthogonal directions (i.e. the two arrays are shifted of $\frac{1}{2} X$ in the x direction
 82 and $\frac{1}{2} Y$ in the y direction). In the second arrangement, which will be named Assembly 2 (A2), the rows of
 83 impingement and effusion holes are aligned, but each hole is shifted of $\frac{1}{2} Y$ in the y direction. Following this
 84 choice of parameters, Assembly 1 presents impingement jets which are almost equally spaced with respect to
 85 the three closest effusion holes, while in Assembly 2 impingement jets are midway each set of four effusion
 86 hole. Both the assemblies are designed in order to maximize heat transfer on the effusion plate cold side.
 87 Figure 3 summarizes the two proposed assemblies, showing the effusion and impingement hole traces on the
 88 effusion plate cold side.

Table 1: Investigated systems geometric characteristics.

Geometry	X/D_i [-]	Y/D_i [-]	Z/D_i [-]	D_e/D_i [-]	S_i/D_i [-]	S_e/D_i [-]	β_i [°]	β_e [°]	N_i [-]	N_e [-]
1	10.5	10.5	6.5	1.0	1.3	2.0	90	20	60	60
2	3.0	3.0	2.5	0.90	0.5	0.7	90	90	54	54

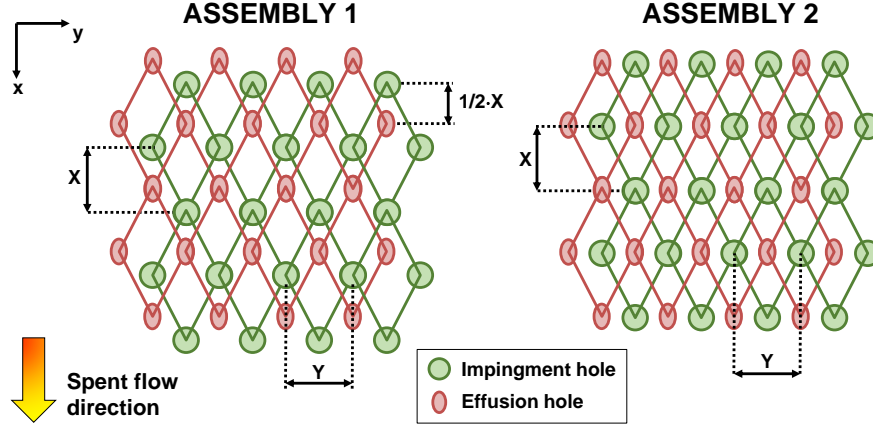


Figure 3: Investigated holes relative positions.

89 To assess the effect of coolant extraction through the effusion holes, heat transfer has also been investigated
 90 for both the impingement arrays paired with a smooth target surface (E0). Coolant extraction is performed
 91 by means of two large slots symmetrically located at the opposite sides of the smooth target plate in the x
 92 direction, with $30 \times 280 \text{ mm}^2$ rectangular cross section. This provides an impingement-only reference case
 93 for each geometry where the impingement flow field is only altered by jet-jet interactions and spent coolant
 94 flow (crossflow).

95 A PMMA plenum with an inner volume of $280 \times 280 \times 320 \text{ mm}^3$ is located downstream of the effusion plate
 96 and collects the spent coolant flow, which is then extracted and directed towards the vacuum system.

97 The aim of this work is to determine heat transfer coefficient distributions on the upstream surface of the
 98 effusion plate (effusion cold side). According to the transient HTC measurement approach, the temperature
 99 distribution of this surface needs to be monitored during the test. Narrow band thermochromic liquid crystals
 100 (TLCs) from LCR Hallcrest with a colour play range between 40°C and 41°C are used for this measurement.

101 The target heat transfer surface is first sprayed with the TLC coating and then with a water base black
 102 paint to provide the TLCs a non-reflecting background. TLCs colour (i.e. surface temperature) evolution
 103 is recorded by means of a Sony XCD-SX90CR CCD camera connected to a PC via IEEE 1394b interface.

104 The camera is located outside of the plenum, and observes the heat transfer surface through the effusion
 105 plate itself and the rear side of the plenum, which is then designed as a transparent PMMA optical window.

106 Illumination is provided by two 8 W white LED arrays, capable of 750-800 lumen each.

107 TLCs have been calibrated in the same optical conditions of the actual test, with the aim to obtain an accu-
 108 rate colour-temperature response. To do so, the steady state gradient method [18] has been employed. TLCs

109 and black coating have been applied to a PMMA sheet of the same thickness as the effusion plate. A 4 mm
 110 thick, rectangular aluminium plate has then been thermally connected to the TLC coated surface by means

111 of a high conductivity compound. A side of the plate is heated by a cartridge electrical heater, while the
 112 opposite side can be cooled down by means of a flow of compressed air. An expanded polyurethane enclosing

113 thermally insulates the whole assembly from the environment. The plate is designed in such a way that a
114 suitable one-dimensional temperature gradient can be set along its length by regulating the heating power
115 and the air mass flow rate. Nine T type thermocouples (0.5 K measurement accuracy), whose locations
116 are exactly known, measure the plate temperature in as many locations, while a camera records the TLC
117 response: in this way, temperature data can be associated with the TLC colour. To improve accuracy in
118 such a narrow temperature band, thermocouples have been recalibrated using a Pt100 RTD (uncertainty
119 $\pm 0.1K$, level of confidence 95%). The employed measurement technique requires that a specific temperature
120 is associated with a precise event: the most repeatable and evident effect resulted to be the green colour
121 peak intensity, which has then been chosen as the colour descriptor. Calibration has been repeated several
122 times in order to improve the reliability of the results. For the lot of TLCs used for the whole experimental
123 campaign, the calibrations showed a mean value of the green peak at $40.5^{\circ}C$ with a standard deviation of
124 $0.13^{\circ}C$.

125 Local temperature measurements is performed in different points of the rig thanks to several T type ther-
126 mocouples, connected to a data acquisition/switch unit (Agilent 34970A). This measurement is needed to
127 determine air temperature evolution, requested by transient technique data reduction: a small thermal in-
128 ertia is thus needed for the sensor to correctly describe air temperature evolution. Air temperature is thus
129 registered by thermocouples with 0.5 mm diameter sheath, directly located upstream the impingement holes:
130 the combination of small sensor volume and high air velocity provides a maximum time constant of around
131 0.5 s for the thermocouples, which is considered satisfactory for the present case.

132 Local pressure measurements are performed thanks to a Scanivalve DSA 3217 pressure scanner, housing 16
133 piezoresistive relative pressure sensors with a maximum accuracy of 6.9 Pa. To replicate the desired flow
134 conditions, air mass flow rate is measured on the extraction line through a calibrated orifice, according to
135 the standard EN ISO 5167-1. Mass flow rate is controlled by varying pumps rotating speed.

136 2.2. Test procedure

As mentioned above, heat transfer tests are performed using a TLC transient technique. The desired flow conditions are initially set by circulating ambient air into the rig, thus keeping the whole geometry at constant ambient temperature. When pressure and mass flow rate reach steady conditions, the camera starts recording (1280×960 resolution at 30 fps) and the mesh heater is switched on, thus causing a quick temperature increase of inlet air (around 1.5 s to reach target temperature, which usually ranges from $55^{\circ}C$ to $65^{\circ}C$). The test is concluded when the maximum of green intensity in every point of the TLC coated surface is reached.

The transient method for convective heat transfer coefficient h evaluation is based on transient heat transfer between a solid surface and a fluid when the latter undergoes an instantaneous temperature change [19, 20]. Under the hypotheses of one-dimensional conduction and semi-infinite solid, surface temperature evolution

is described by:

$$\frac{T_w(t) - T_{init}}{T_j - T_{init}} = 1 - \exp\left(\frac{h^2 \alpha t}{k^2}\right) \operatorname{erfc}\left(\frac{h\sqrt{\alpha t}}{k}\right) \quad (1)$$

where T_w is the surface temperature, T_{init} is the initial wall temperature, T_j is the air temperature, t is the time elapsed from air temperature change and α and k are thermal diffusivity and conductivity of the wall material (PMMA in the present case) respectively. Heat transfer distribution can be calculated from equation 1 if the time employed to reach the green peak intensity is known for every point of the TLC coated surface, as well as the temperature corresponding to this peak (thanks to TLC calibration). Since in the present case a quick, yet not instantaneous air temperature step could be realized, the principle of superimposition has been applied to model gas temperature profile in the data reduction procedure, according to the approach firstly introduced by Metzger and Larson [21]. The semi-infinite solid hypothesis has been verified by ensuring that test duration is lower than the limit condition proposed in the study of Vogel and Weigand [22] ($t_{max} < S_e^2/4\alpha$ for the present case).

Flow conditions are identified by the impingement jet Reynolds number, defined as:

$$Re_j = \frac{\dot{m}D_i}{N_i A_i \mu} \quad (2)$$

where \dot{m} is overall air mass flow rate, A_i is the impingement hole cross-section and μ is air dynamic viscosity. Since target temperature establishes quickly after the beginning of the test and undergoes little variation along the test duration, jet Reynolds number can be considered as constant during the test and thus air dynamic viscosity is calculated at the target temperature itself. Reynolds numbers ranging from around 2500 to 10000 have been investigated for Geometry 1, while Geometry 2 has been tested at a single Re_j value of around 15700. Convective heat transfer coefficient values have been reformulated in a dimensionless form as Nusselt number values, defined as:

$$Nu = \frac{hD_i}{k_j} \quad (3)$$

137 where k_j is air thermal conductivity, evaluated using the almost constant air temperature after the step
 138 change. For every test, the results in terms of Nusselt number distributions will be presented normalized
 139 as Nu/Nu_0 , where Nu_0 is the area averaged Nu measured in Row 1 regions (see Figure 6(a)) for the test
 140 configuration with Geometry 2 impingement plate (I2) and smooth target surface (E0) at around $Re_j = 15700$.
 141 As will be discussed in the validation section this value is in agreement with the averaged Nusselt number
 142 (around 7% below) predicted by Bailey and Bunker correlation [23] of Eq. 4. This kind of normalization
 143 allows to appreciate the relative difference between the geometries and the relative impact of flow conditions.

144 2.3. Measurement uncertainty

145 The uncertainty analysis has been performed according to the standard ANSI/ASME PTC 19.1 [24],
 146 based on the Kline and McClintock method [25]. A maximum uncertainty of 2.2% has been determined
 147 on Re_j , with typical values ranging from 1.5% to 2%. The same approach has been applied to the Nusselt
 148 number evaluation. Given the employed technique, the uncertainty on Nu depends upon the accuracy of

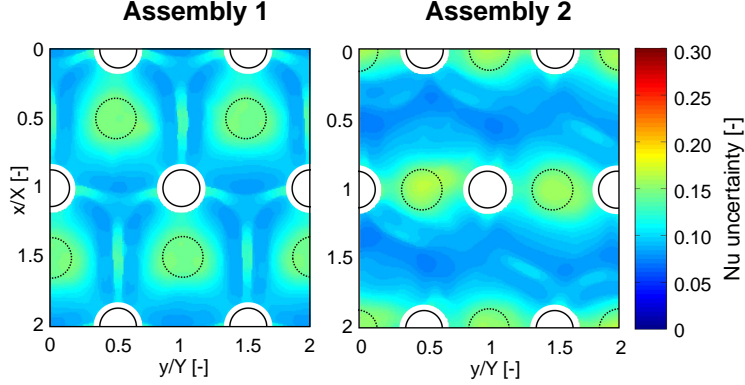


Figure 4: Uncertainty distribution for Geometry 2 at around $Re_j = 15700$.

149 temperature measurement (both of TLCs and thermocouples) and wall material properties, as well as upon
 150 measurement sample rate (given by data acquisition and camera framerate). Figure 4 presents a typical
 151 distribution of Nu measurement uncertainty.

152 For local Nu values, uncertainty can be as high as 20% where heat transfer peaks occur (due to the quick
 153 TLC colour play), while values below 10% are present in every other region. Maximum uncertainty on area
 154 averaged Nu value is 12% (for Geometry 1, Assembly 1 test at around $Re_j = 7400$), with typical values
 155 falling in the range of about 8-10%. All the reported uncertainties bounds are based on a 95% confidence
 156 level.

157 3. CFD MODELLING

158 As a consequence of the employed experimental setup, heat transfer measurement is not possible or at
 159 least not reliable in some localized regions, e.g. in the zones covered by the Geometry 1 inclined effusion
 160 holes (due to limited optical access) and in the immediate surroundings of effusion holes (due to unreliability
 161 of one-dimensional transient heat transfer hypothesis). This issue suggests the opportunity to exploit CFD
 162 to have a better insight into the actual heat transfer distribution and flow field. Steady RANS and unsteady
 163 SAS calculations were carried out for the four impingement/effusion configurations.

164 Figure 5 presents a sketch of the computational domain for Geometry 1 (IIE1). Mass flow rate and total
 165 temperature were assigned as inlet boundary conditions, whereas a static pressure condition was prescribed
 166 at the outlet. The prescribed values are derived from the ones measured during the experiments, chosen in
 167 order to match the average jet Reynolds number. All the walls were treated as smooth and adiabatic with a
 168 no slip condition, except for the target plate, to which a constant temperature condition was applied. The
 169 translational geometrical and fluid dynamic periodicity allows to reduce the computational effort considering
 170 only two rows in the streamwise (x) direction. A further reduction is achieved imposing a symmetry condition
 171 in the spanwise (y) direction.

172 ANSYS Meshing was used to generate the computational grid depicted in Figure 5. A quite coarse sizing
 173 was used in the plena, while a mesh refinement was performed in the proximity of the impingement and

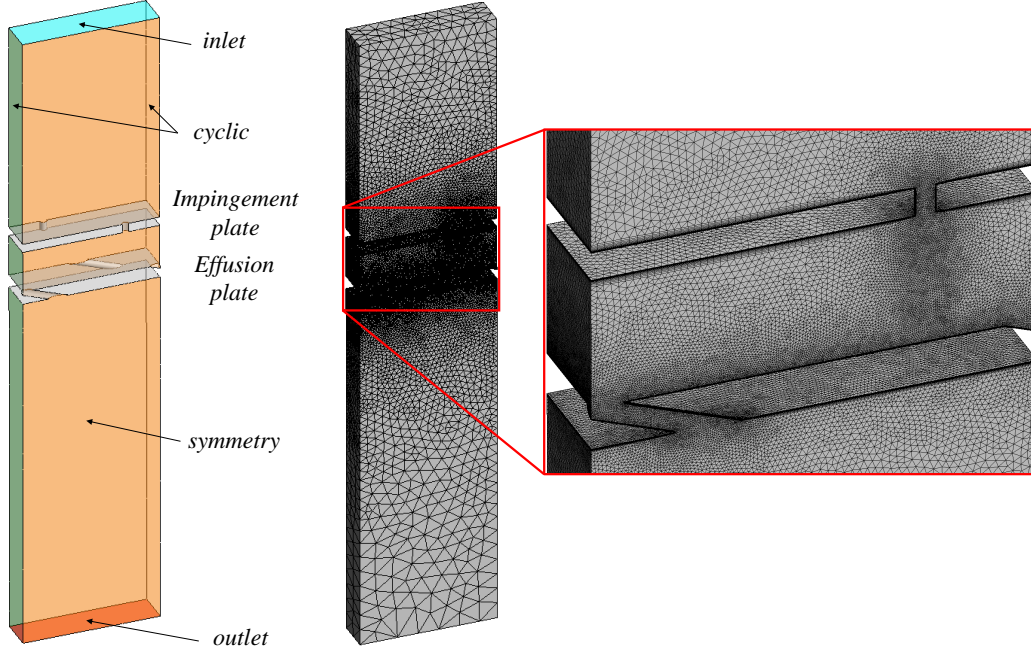


Figure 5: Computational domain and grid.

174 effusion holes, according to the results of a mesh sensitivity analysis (not reported for the sake of brevity).
 175 Approximately, fifteen elements per hole diameter were used to discretize the perforations. The hybrid
 176 unstructured mesh is composed of tetrahedrons and a layer of 15 prisms on the target plate, thus retrieving
 177 a y^+ value around unity. As a result, computational grids with a total of $4.3 - 4.5 \cdot 10^6$ elements and
 178 $1.10 - 1.25 \cdot 10^6$ nodes were generated.

179 Steady RANS calculations were carried out using the Navier-Stokes solver ANSYS Fluent v16.2. The fluid
 180 was treated as an ideal gas with variable properties: thermal conductivity, dynamic viscosity and specific
 181 heat capacity at constant pressure are considered temperature-dependent. The second order upwind scheme
 182 was used to provide an accurate solution. Turbulence was treated using the $k - \omega$ SST turbulence model,
 183 which, according to Zuckerman and Lior [26] provides the most accurate results among two-equation eddy
 184 viscosity models. The Wall Integration treatment was recovered thanks to the cell clustering on the surfaces
 185 of interest.

186 Geometry 2 Assembly 1 was also investigated using an unsteady CFD approach, since results have shown that
 187 this configuration is strongly influenced by flow unsteadiness effects (see section 4.3). In this case, turbulence
 188 modelling was treated using Scale Adaptive Simulation (SAS), a hybrid RANS-LES model, in its formulation
 189 made available by the solver, based on the $k - \omega$ SST model. In this calculation, the computational domain
 190 has the same streamwise extension of the one employed with RANS simulations, but is twice as wide in
 191 the spanwise direction, in order to remove the symmetry plane boundary. A whole impingement jet is
 192 thus simulated, obtaining a complete solution of the associated unsteady flow structures. This resulted in
 193 a computational grid composed of $8.96 \cdot 10^6$ elements. The simulation was carried out with a time step

194 of $2 \cdot 10^{-5} s$, derived from a sensitivity analysis and resulting to be small enough to provide a convective
 195 Courant number lower than 1. Equations were discretised with the Second Order Backward Euler scheme.
 196 The simulations were stopped after $6 \cdot 10^{-3} s$, a time frame able to consider at least three passages through
 197 the impingement cavity.

198 4. RESULTS

199 The obtained experimental and numerical results will be presented in the following paragraphs. Given
 200 the relevant number of geometries and configurations, all the cases investigated in this work are summarized
 in Table 2.

Table 2: Summary of the investigated geometries and conditions.

Impingement	Effusion	Assembly	jet Reynolds number
	E0	–	7400
I1	E1	A1	2500-5000-7400-10000
		A2	2500-5000-7400-10000
I2	E0	–	15700
		A1	15700
	E2	A2	15700

201

202 4.1. Validation

The results presented in the following paragraphs are obtained from the first experimental campaign performed on the test rig described above: as a consequence, experimental procedure and results need to be validated. This goal is achieved by comparing the results obtained with impingement only configuration with the outcomes of impingement correlations available in the open literature. In particular, Bailey and Bunker [23] correlation is considered, since the configuration obtained combining impingement plate 2 (I2) with the smooth target surface (E0) presents geometric parameters which correspond to its validity ranges. Using the nomenclature employed in this work, the correlation can be expressed as:

$$\begin{aligned}
 Nu = 47.1 - 5.5 \frac{X}{D_i} + \frac{Z}{D_i} \left(7.3 - 2.3 \frac{Z}{D_i} \right) + Re_j \left(4e-3 - 1.3e-4 \frac{X}{D_i} - 1.5e-8 Re_j \right) + \\
 + \frac{G_c}{G_j} \left(61.2 - 13.7 \frac{X}{D_i} - 28 \frac{Z}{D_i} \right) \quad (4)
 \end{aligned}$$

203 where G_c and G_j represent the ratios of mass flow rate and passage area for impingement cavity and jet
 204 respectively. Nu/Nu_0 distribution for the test performed at around $Re_j = 15700$ is reported in Figure
 205 6(a) The two extraction slots (located above and below the surface presented in Figure 6(a)) give birth
 206 to a symmetric flow field, which can be identified as generated by two equal, undisturbed impingement

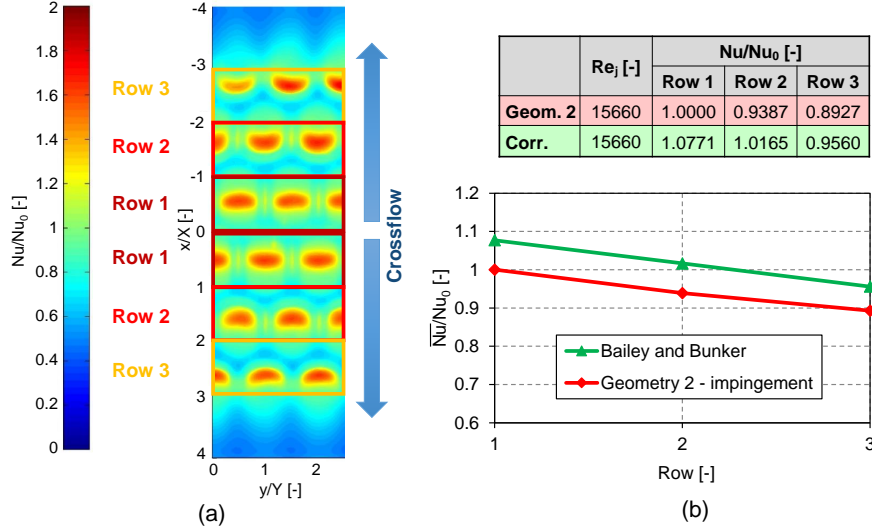


Figure 6: Nu/Nu_0 distribution at around $Re_j = 15700$ test (a) and comparison with Bailey and Bunker [23] correlation (b).

arrays. According to that, the row-by-row area averaged Nu values reported in Figure 6(b) are calculated as the average of each couple of symmetric rows. It can be noticed that data from the experiment and the correlation agree both in terms of values and trends, with discrepancies falling below the experimental uncertainty (differences ranging from 7.1% to 8.3%). The experimental procedure and apparatus employed in the present study can thus be considered validated.

4.2. Geometry 1

Figure 7 reports Nu/Nu_0 distributions for Geometry 1 for the two hole assemblies obtained at $Re_j = 7400$. Geometry 2 results will be presented separately in the following paragraphs, given the considerable differences between the two configurations. The effusion holes locations (solid lines) and the projections of impingement holes on the investigated surface (dashed lines) are also reported in the maps. White areas hide the regions which are covered by the inclined effusion holes. Moreover, also the regions in which the hypotheses of transient technique (one dimensional conduction and semi-infinite wall) are not verified are excluded from the analysis.

Jet-jet interactions seem to be weak, likely due to the relatively high jet-to-jet distance. The effect of impingement/effusion holes relative positioning can be appreciated by comparing the maps corresponding to the two investigated assemblies, reported in Figure 7. The general shape of heat transfer pattern is similar for the two assemblies. By observing the distributions in closer detail, a slight shift of the peak location towards the positive x direction can be identified for Assembly 2, as well as a less circular shape of the Nu/Nu_0 distribution, which seems to be slightly enlarged towards the same positive x direction. If heat transfer magnitude is considered, significantly higher Nu/Nu_0 peaks are recorded for Assembly 1, while far from the jet stagnation and around the effusion holes only slight differences can be noticed.

Considering Figure 8, it can be noticed that both the pattern and the magnitude of Nu contours are

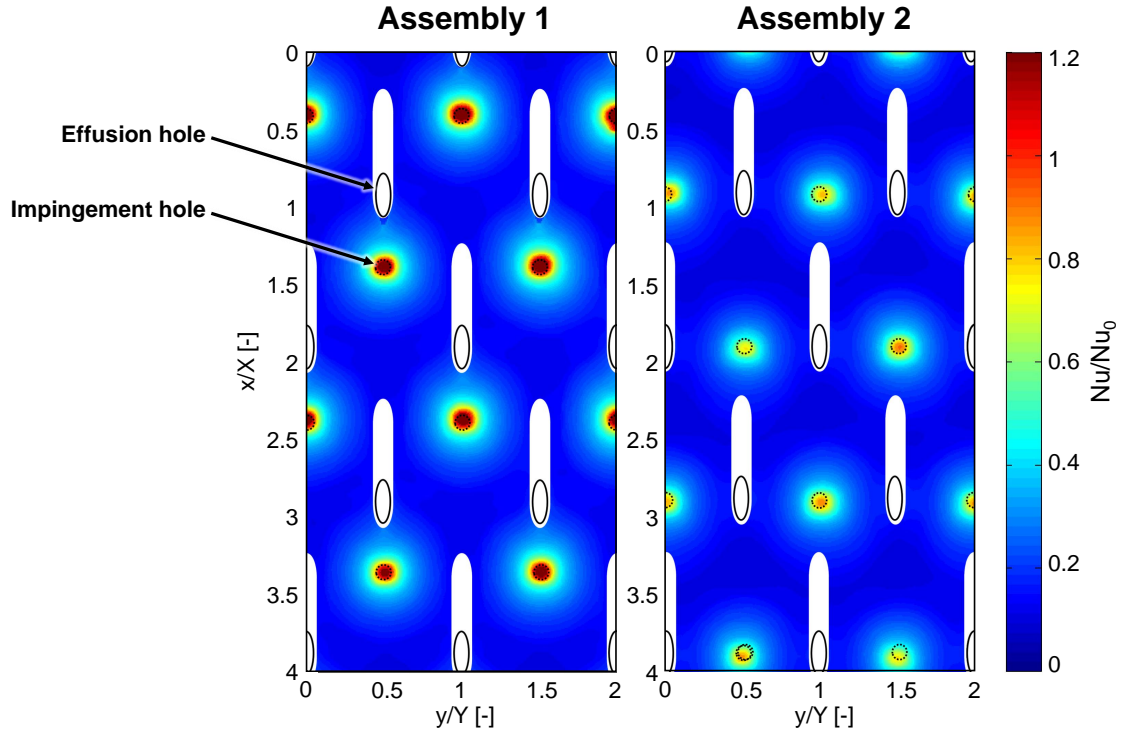


Figure 7: Nu/Nu_0 distributions for Geometry 1 (I1E1) at around $Re_j = 7400$.

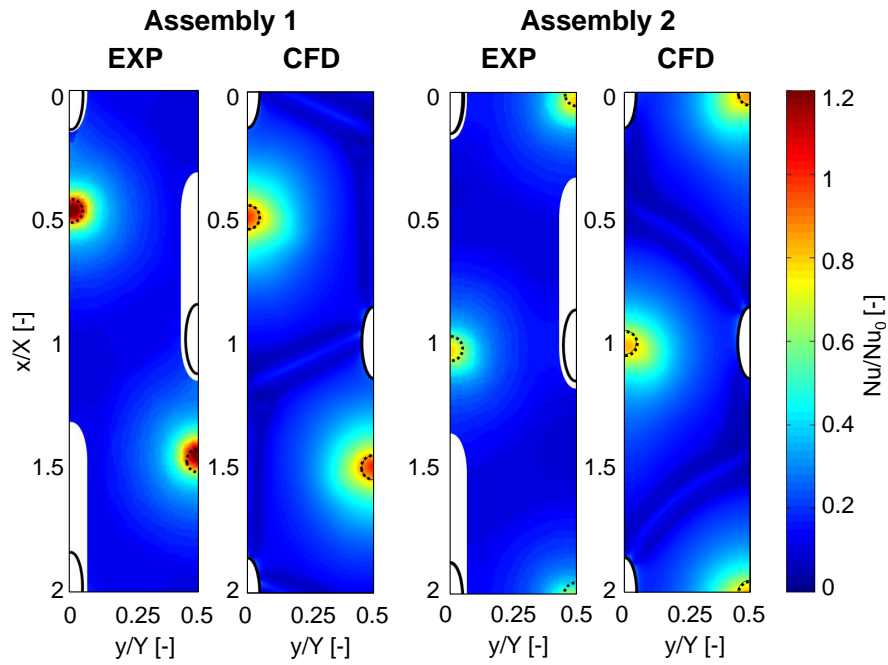


Figure 8: Nu/Nu_0 distributions for Geometry 1 (I1E1) at around $Re_j = 7400$: comparison between experimental data and RANS simulations.

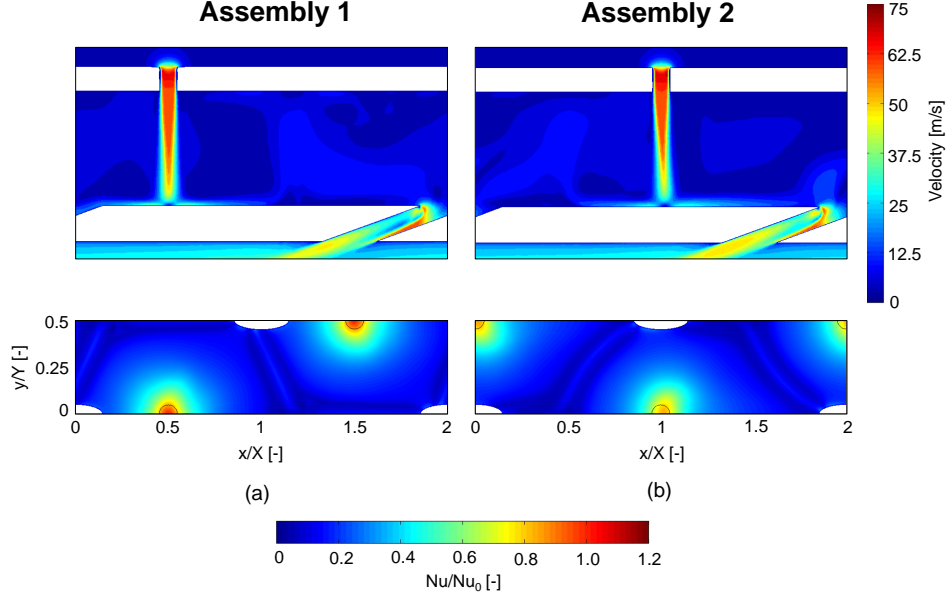


Figure 9: Flow field predicted by CFD for Geometry 1 (I1E1) at around $Re_j = 7400$.

229 reasonably replicated by the RANS CFD simulations: local discrepancies with experimental data range
 230 from 5% to 17% and average discrepancies are approximately 10% (corresponding to an absolute difference
 231 $|Nu_{CFD} - Nu_{exp}|$ of around 1.7), which fit within the expected values for the computational model [26] and
 232 are very close to the experimental uncertainty. This allows to exploit numerical predictions for obtaining a
 233 better understanding of flow physics within the system and its impact on heat transfer characteristics.

234 The predicted flow field provided by CFD on the left symmetry plane ($y/Y = 0$) is reported in Figure 9. Some
 235 interesting discussions can be drawn if this is analysed together with the corresponding heat transfer map.
 236 The different Nu peak value of the two assemblies near the jet stagnation region can be interpreted as the
 237 effect of the impingement/effusion pattern characteristic of each assembly. For Assembly 1 the impingement
 238 jet appears to be approaching the target plate following a straight trajectory, an expected behaviour due
 239 to the absence of a dominant crossflow. Nevertheless, when the Assembly 2 is considered, the jet seems
 240 to be attracted by the right effusion row, leading the impingement jet to be bent towards the positive x
 241 direction. This phenomenon is similar to the typical effect of a crossflow, as confirmed by the kidney-shaped
 242 stagnation region (see for example Row 3 in Figure 6), hence justifying the detrimental impact on the cooling
 243 performance observed for the Assembly 2 configuration.

244 4.2.1. Effect of Reynolds number

245 The effect of different mass flow rates on impingement/effusion cooling performance has been assessed by
 246 performing four tests for each Geometry 1 assembly, so to explore Re_j values ranging from 2500 to 10000.
 247 Figure 10 presents the Nu/Nu_0 distributions obtained with Assembly 2. The maps show that a variation
 248 of Re_j in the investigated range does not significantly influence the shape of Nu/Nu_0 pattern, while heat
 249 transfer is enhanced by an increase of Re_j in every point of the map. Similar considerations can be derived

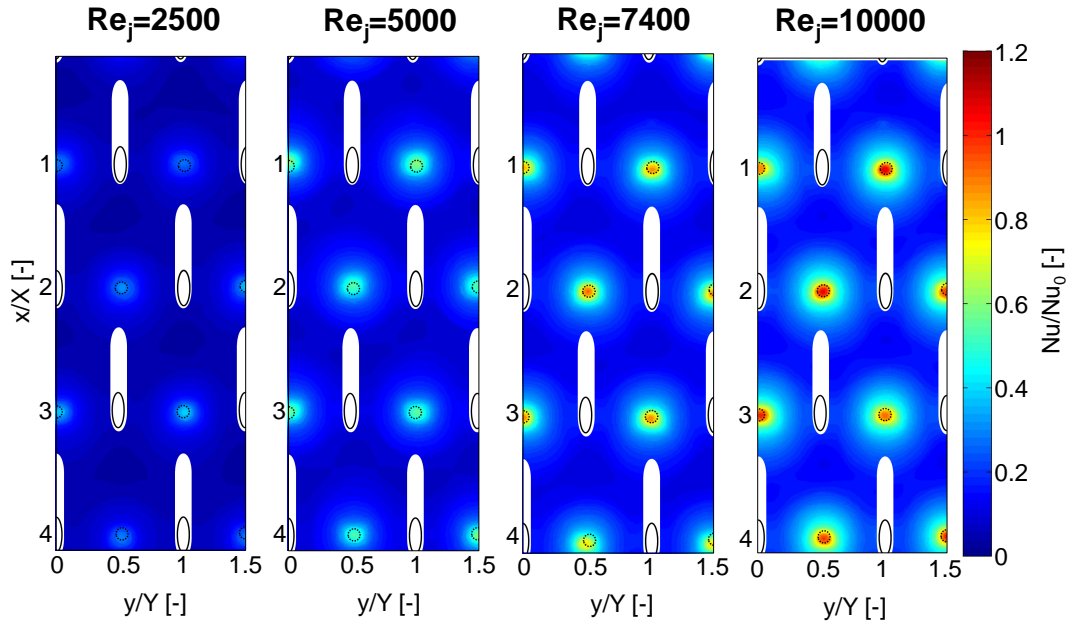


Figure 10: Nu/Nu_0 distributions for Geometry 1, Assembly 2 for different Re_j values.

250 from the Assembly 1 distributions, which are not reported for the sake of brevity.
 251 More comprehensive conclusions can be drawn by comparing area averaged Nu values for the investigated
 252 cases. As shown in Figure 11, Assembly 1 outperforms Assembly 2 for the whole Re_j range, which demon-
 253 strates that the flow phenomena causing this difference occur in every investigated condition. Even so, the
 254 relative differences decrease from 33% to 19% as Re_j increases, and the power law fittings of the two data
 255 sets (solid lines) show a slightly stronger heat transfer enhancement of Assembly 2 as Re_j grows. The latter
 256 fact seems to indicate that the detrimental effect of locally induced jet deflection verified for Assembly 2 is
 257 reduced if impingement jet momentum increases.

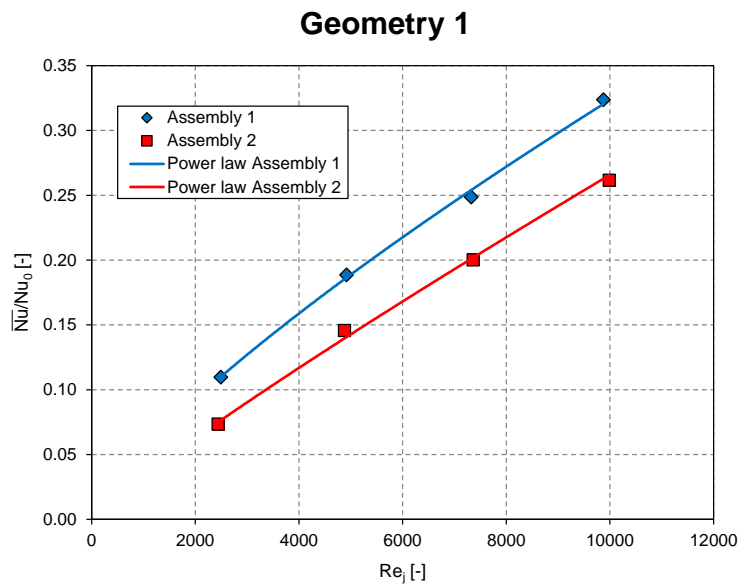


Figure 11: Effect of Re_j on area averaged Nu values for Geometry 1.

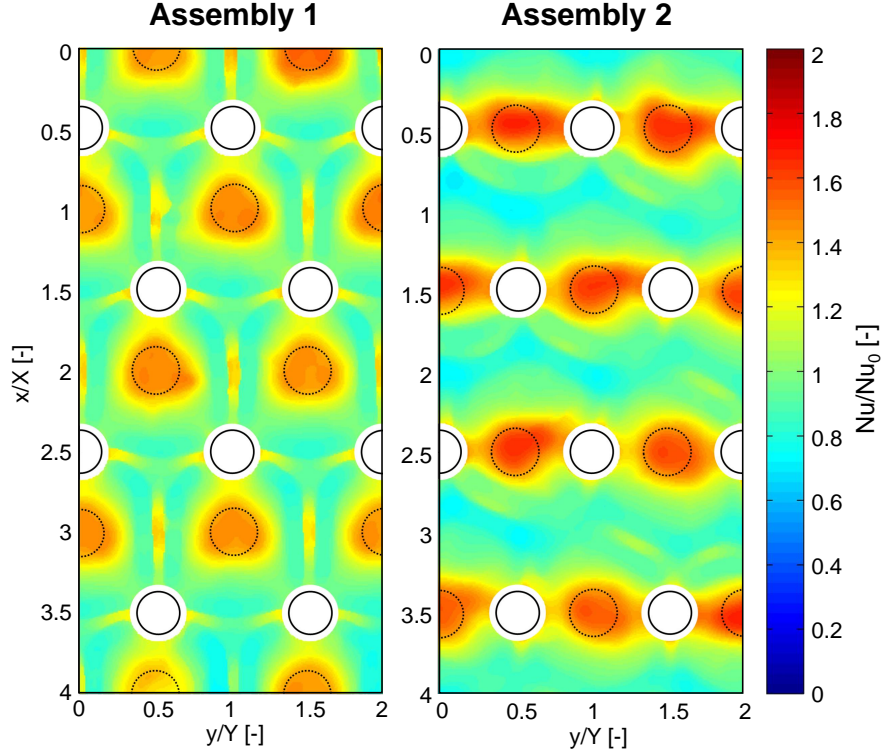


Figure 12: Nu/Nu_0 distributions for Geometry 2 (I2E2) at around $Re_j = 15700$.

258 4.3. Geometry 2

259 Figure 12 presents Nu/Nu_0 distributions obtained for Geometry 2 at around $Re_j = 15700$ for the two
 260 investigated assemblies. Effusion holes locations and impingement holes projections are reported with the
 261 same notation of Figure 7.

262 For this geometry, the presence and location of effusion holes strongly influence the spatial distribution of
 263 heat transfer. For Assembly 1 a heat transfer peak is located below each impingement hole, as recorded for
 264 Geometry 1: in this case, however, a rather complex pattern of this region is observed, with three distinct
 265 lobes directed towards the nearest effusion holes. This behaviour also extends to the surroundings of the
 266 stagnation region. Secondary high heat transfer regions are present between adjacent jets, elongated in
 267 shape and perpendicular to a line linking the jet impact locations. The cause of these secondary peaks can
 268 be identified in the fountain effect, already observed and described by Cho and Rhee [5]. According to the
 269 scheme of Figure 13, two primary vortices arise in the region between two impingement jets, and a low heat
 270 transfer region is generated where they detach from the wall. In between the detachment locations two
 271 secondary vortices are formed, whose recirculation creates a local impingement effect and thus an increase
 272 in heat transfer. A heat transfer peak under each impingement jet is also present for Assembly 2, but its
 273 magnitude is significantly higher than the ones recorded for Assembly 1. The peak shape is also deeply
 274 different, being elongated in the direction of the two nearest effusion holes. At the same time, low heat
 275 transfer zones are present in the areas between the effusion holes rows ($x/X \simeq 1, 2, 3$). This can be ascribed
 276 to weak and unstable jet-jet interactions occurring in these areas. For Assembly 2 map, Nu peaks related to

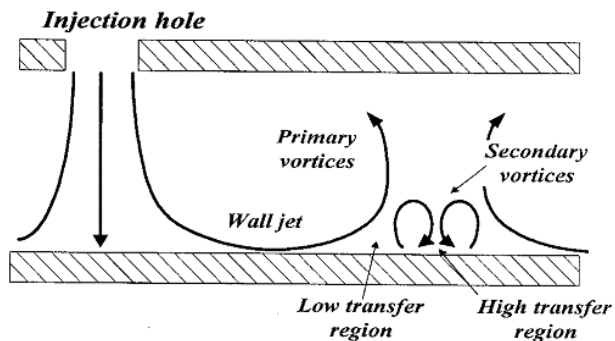


Figure 13: Sketch of the fountain effect (Cho and Rhee [5])

277 fountain effect are located close to the lower side of the jets in the left part of the distribution ($x/X < 1 - 1.5$)
 278 and close to the upper side on the right part ($x/X > 1 - 1.5$). Multiple repetitions of the test show that these
 279 regions randomly migrate close to the higher or lower side of the jets. These observations can be justified
 280 considering the impingement/effusion pattern features. In this case, the low distance between impingement
 281 and effusion holes results in a strong interaction between jets and coolant extractions, while far from the
 282 rows the lack of confinement induces the generation of flow unsteadiness. This phenomenon is inhibited for
 283 Assembly 1, where the coolant bleeding provided by effusion holes tends to stabilize this region, increasing
 284 at the same time the heat transfer. Overall, this effect compensates for the reduction of the stagnation
 285 region peak value, resulting in an average Nu/Nu_0 of 1.07 for Assembly 1 and 1.08 for Assembly 2. Roughly
 286 the same heat transfer entity is thus measured for the two configurations, but with a more homogeneous
 287 distribution for Assembly 1. This conclusion is coherent with literature results (Cho and Rhee, [5]).

288 Also CFD simulations performed for Geometry 2 show a reasonable agreement with experimental data,
 289 correctly reproducing the heat transfer pattern in the stagnation region (Figure 14). In the areas of jet-jet
 290 interactions, i.e. where the fountain effect occurs, the correct behaviour is still replicated, even though CFD
 291 solution shows higher gradients with respect to the measured ones. This discrepancy can be interpreted by
 292 considering that steady RANS approach returns a frozen solution of a highly unstable aero-thermal field,
 293 while in the experimental outcomes only the time averaged result of such unsteadiness is recorded, leading
 294 to a smoother measured heat transfer pattern. This fact is particularly evident for Assembly 2, where steady
 295 RANS solution omits the time evolution of the unsteady and randomly migrating jet-jet interaction regions
 296 and provides instead an ideal, instantaneous Nu pattern. Even so, quantitative agreement is reasonable
 297 in both cases, with discrepancies on area averaged heat transfer falling below $\pm 5\%$ ($+2.5\%$ for Assembly 1,
 298 corresponding to a difference $Nu_{CFD} - Nu_{exp} = 2.2$, and -4.5% for Assembly 2, corresponding to a difference
 299 $Nu_{CFD} - Nu_{exp} = -4.0$). The predicted flow fields shown in Figure 15 clearly highlights a similarity with
 300 the sketch in Figure 13, with the formation of the primary and secondary vortices typical of fountain effect.
 301 As expected, a much better agreement is present between the results of the experiment and of the scale
 302 resolving SAS simulation. In Figure 16 the experimental Nu/Nu_0 distribution on the effusion plate cold side

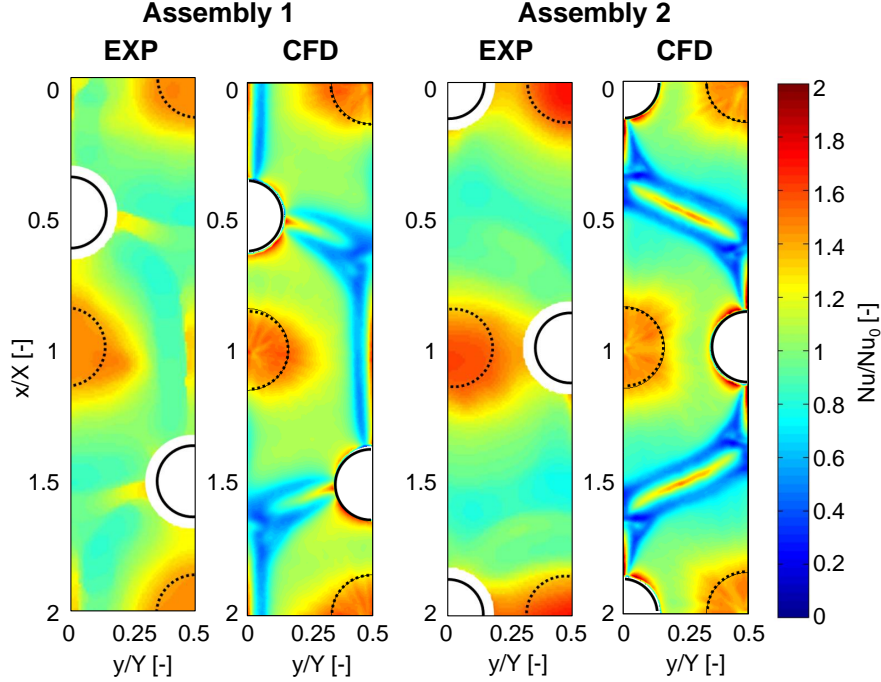


Figure 14: Nu/Nu_0 distributions for Geometry 2 (I2E2) at around $Re_j = 15700$: comparison between experimental data and RANS simulations.

303 is compared with the time averaged one obtained from the simulation. Slightly higher heat transfer gradients
 304 and a small overestimation of the peaks are still present in the computed distribution, but the pattern shape
 305 and entity are replicated in a more accurate way with respect to the steady RANS simulation. In fact, the
 306 scale resolving simulation allows to determine the role of unsteadiness in defining the heat transfer pattern:

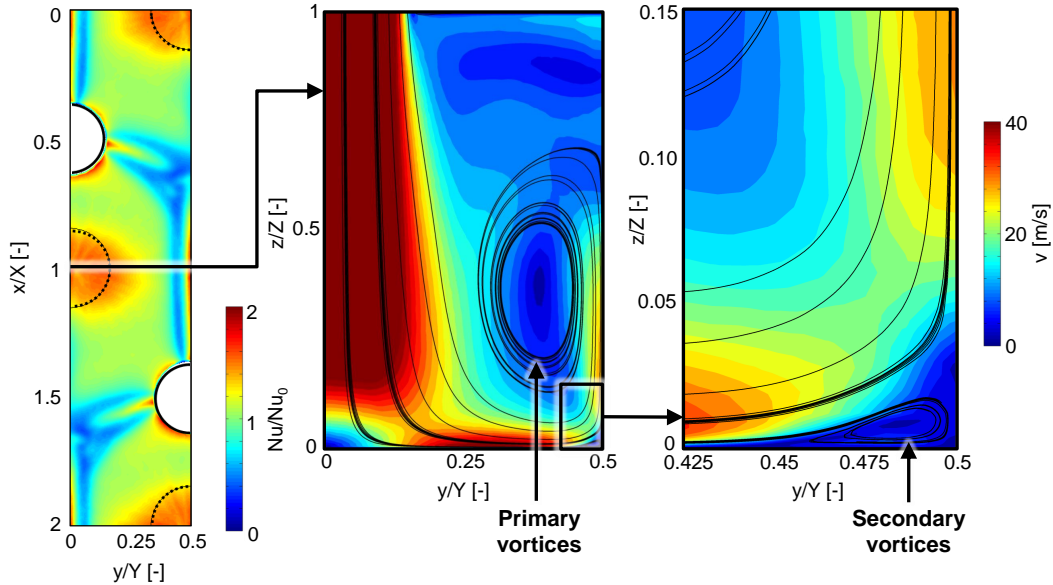


Figure 15: Flow field predicted by CFD for Geometry 2 Assembly 1 (I2E2-A1) on the $x/X = 1$ plane at around $Re_j = 15700$.

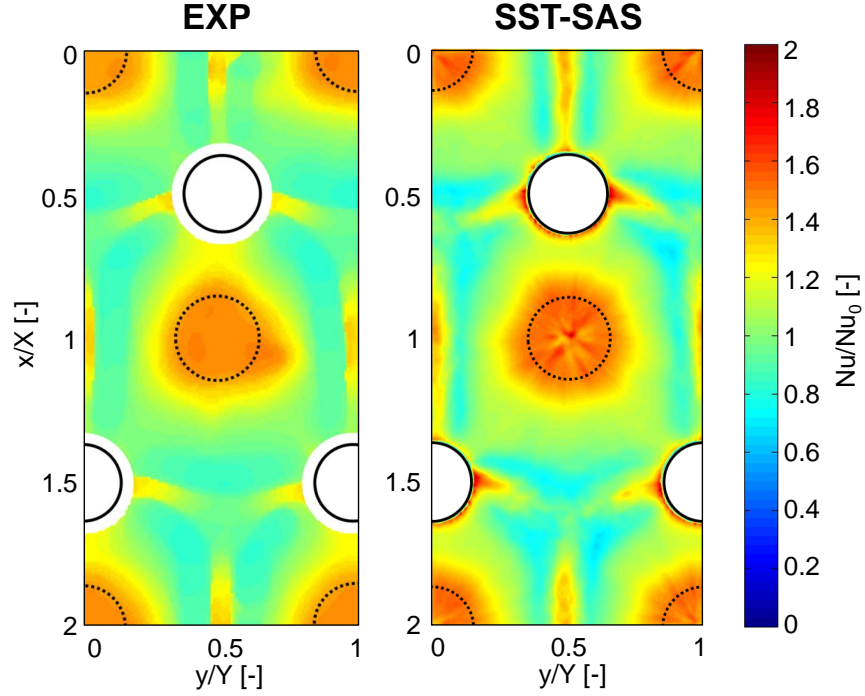


Figure 16: Nu/Nu_0 distributions for Geometry 2 Assembly 1 (I2E2-A1) at around $Re_j = 15700$: comparison between experimental data and SAS simulation.

307 as shown in Figure 17, the instantaneous heat transfer distribution is strongly different from the average
 308 pattern, which is the one provided by the experiments. This is due to the large scale shear layer vortices
 309 (also visible in Figure 17), which get transported by the jet, impact onto the target surface, travel along the

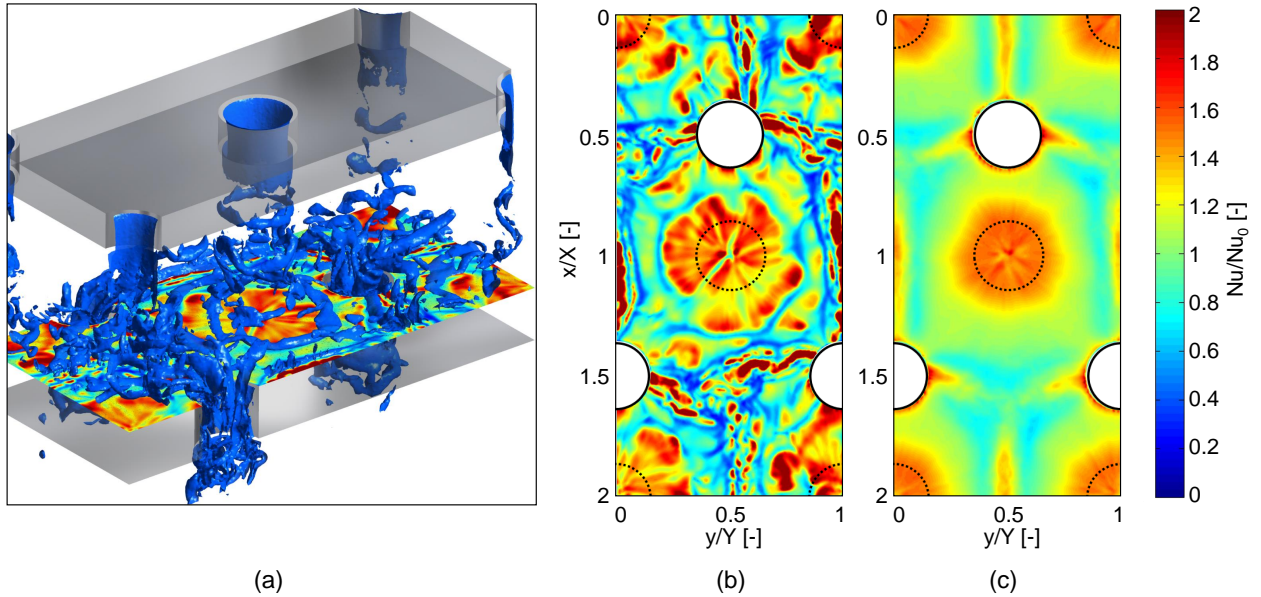


Figure 17: Effect of flow unsteadiness on heat transfer for Geometry 2 Assembly 1 (I2E2-A1) at around $Re_j = 15700$: vorticity structures (a) and corresponding instantaneous Nu/Nu_0 distribution (b) compared with the time averaged one (c).

310 wall jet region and then interact with the corresponding structures coming from the surrounding jets. Strong
 311 local differences in heat transfer are generated by a similar flow field, which move along the surface together
 312 with the corresponding flow structures. In this case, the computed area averaged Nu value is around +9.6%
 313 higher than the experimental one, which is a higher value than the one recorded for the RANS simulations:
 314 this is due to the fact that the strong underestimation of low heat transfer regions of the RANS compensated
 315 the overestimation of heat transfer peaks in average terms. This does not happen for the SAS simulation,
 316 where the low heat transfer zones are correctly replicated and thus the recorded discrepancy only reflects the
 317 slight overestimation of the peak regions. In any case, it should be noticed that all of these discrepancies are
 318 comparable with the evaluated experimental uncertainty.

319 4.4. Effect of coolant extraction

320 Impingement 1 and 2 geometries (I1 and I2) have also been tested with a smooth target surface (E0),
 321 with the aim to study the effect of coolant extraction on impingement heat transfer. Figure 18 and Figure 19
 322 show the outcomes of such investigation, together with the corresponding heat transfer distributions obtained
 323 with effusion holes. The maps show that significantly different effects of coolant extraction are present for
 324 the two geometries. For Impingement 1 (Figure 18) effusion holes seem not to dramatically alter the heat
 325 transfer pattern shape. However, if the distributions are observed in closer detail, particular similarities can
 326 be identified for the first row ($0 < x/X < 1$) between I1E0 and I1E1-A1 configurations, since the peak
 327 region appears to be almost circular in shape and the peak is located directly under the impingement hole
 328 in both cases. For the I1E0 configuration the first row can be considered unaffected by the spent coolant
 329 flow (crossflow), which is directed towards the positive x direction: this fact confirms that effusion holes in
 330 Assembly 1 configuration do not significantly deflect the impingement jet. As a consequence, the main effect
 331 of coolant extraction consists in the higher peak Nu/Nu_0 values: this can be due to a stabilizing effect of
 332 coolant extraction on the jet itself, which reduces spent coolant flow recirculation (Hollworth et al. [8]) and
 333 interaction with the free jet region and thus minimizes jet momentum losses, which in this case can play a
 334 significant role given the relevant jet-to-target plate spacing ($Z/D_i = 6.5$).

335 A similar heat transfer pattern is also present for the third row ($2 < x/X < 3$) between I1E0 and I1E1-A2
 336 configurations: in both cases, a slightly kidney-shaped Nu/Nu_0 distribution is present near the jet impact
 337 location, and peak values are also similar. For I1E0 configuration the crossflow generated by the first two
 338 rows of impingement holes runs over the third one, deflecting the jets and decreasing heat transfer (Bailey and
 339 Bunker [23]). In the present case, the effect is slight due to the considerable jet-to-jet distances and height of
 340 impingement cavity. This fact however confirms the hypothesis, already highlighted by CFD results, that the
 341 lower values of Assembly 2 with respect to Assembly 1 are due to a phenomenon similar to a local crossflow,
 342 due to the effusion holes themselves.

343 For Impingement 2 (Figure 19), the effect of effusion holes on Nu/Nu_0 distribution shape is significant.
 344 Without effusion holes (I2E0), Nu/Nu_0 peaks elongated in the y direction are recorded, as well as secondary

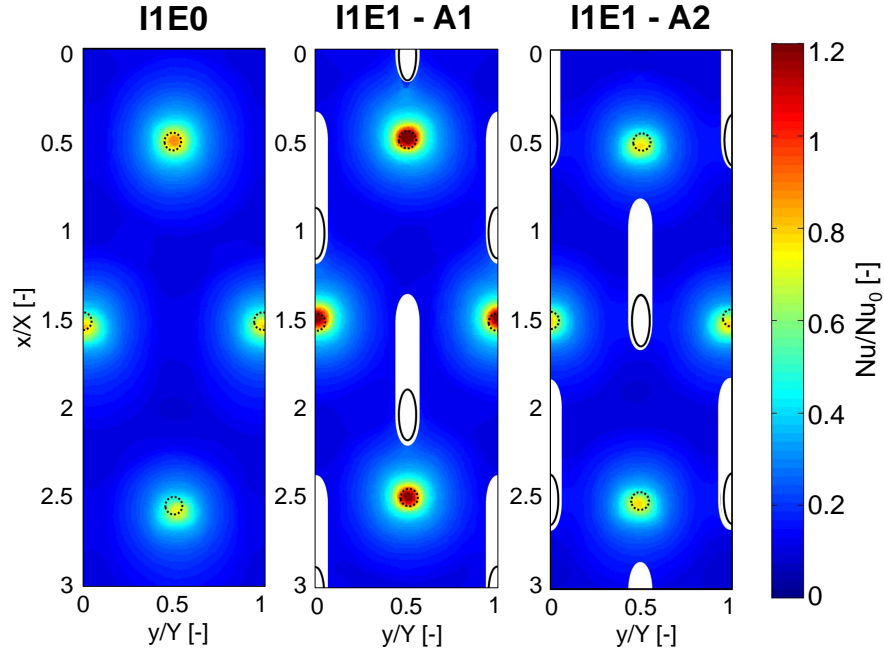


Figure 18: Nu/Nu_0 maps for Impingement 1 geometry (I1) at around $Re_j = 7400$.

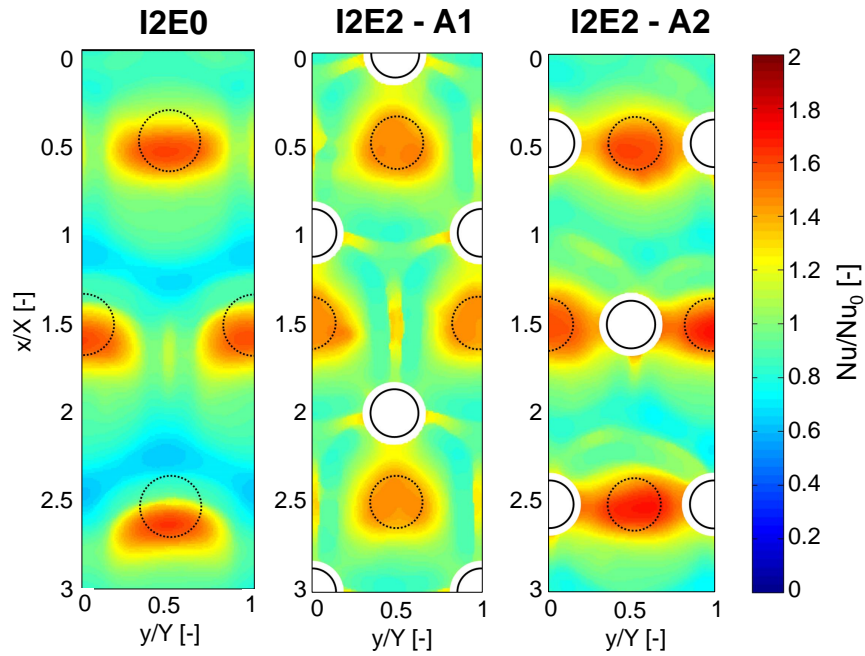


Figure 19: Nu/Nu_0 maps for Impingement 2 geometry (I2) at around $Re_j = 15700$.

345 peaks related to the fountain effect in between adjacent holes of the same row (i.e. at the same x/X values).
 346 Given the low X/D_i , Y/D_i and Z/D_i , crossflow is expected to be significant, and its main effects can be
 347 identified in the shape and the shift of the second and third rows peaks and in the clear decrease in peak
 348 Nu/Nu_0 values. The absence of secondary peaks in between different impingement rows (i.e. with different

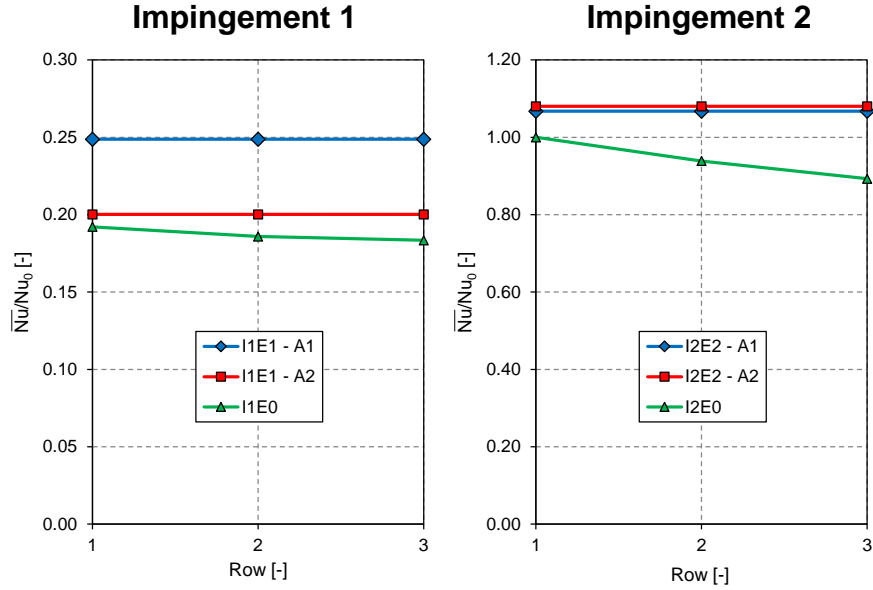


Figure 20: Area averaged Nu/Nu_0 values for Impingement 1 and 2 with different target plates and assemblies.

349 x/X values) could also be interpreted as an effect of the crossflow. If coolant extraction is considered, the
 350 positive effect of effusion holes can be observed, causing an increase in secondary peak magnitude and thus
 351 in heat transfer uniformity (I2E2-A1) or directly enhancing primary peaks (I2E2-A2).

352 Further discussions can be drawn out if area averaged Nu/Nu_0 values for the cases presented in Figure 18
 353 and Figure 19 are considered (Figure 20): for the cases without effusion, Row 1 values are averaged over the
 354 area defined by $0 < x/X < 1$, Row 2 over $1 < x/X < 2$ and Row 3 over $2 < x/X < 3$, while for the sake
 355 of clarity area averaged values over the whole surface are reported for the cases with effusion (i.e. constant
 356 values of Nu changing the Row number).

357 For Impingement 1, the slight decrease of E0 values from Row 1 to Row 3 is due to the crossflow: with
 358 respect to this baseline case, I1E1-A1 presents area averaged values increases ranging from 29.4% (for Row
 359 1) to 35.6% (for Row 3), while I1E1-A2 presents augmentations ranging from 4.2% (for Row 1) to 9.1% (for
 360 Row 3). A clear positive effect of coolant extraction is thus recorded for A1 configuration, while comparable
 361 values are present for A2. It must be noticed that the effusion holes traces limit the optical access on the
 362 measurements surface and hence the area averaged values for the cases with effusion holes are not evaluated
 363 on the whole target surface. However, CFD results allowed to verify that area averaged values decrease of
 364 around 7% in the worst case as the effect of such approximation, thus the considerations reported above are
 365 still valid.

366 The results obtained with I2 plate present a stronger crossflow effect with respect to I1 for the impingement
 367 only configuration (I2E0), since average heat transfer values strongly decrease passing from Row 1 to Row 3.
 368 Values are very similar for the two configurations with effusion holes (I2E2-A1 and A2), as already highlighted
 369 before. Considering Row 1, only a slight increase in heat transfer is present with effusion holes (below 8%),

370 while stronger increases are recorded for Rows 2 and 3 (up to 21%). This leads to the conclusion that the
371 positive effect of effusion holes for Impingement 2 is mainly bound to the suppression of crossflow.

372 5. CONCLUSIONS

373 In this study, two different impingement/effusion cooling systems have been analysed, with the aim to
374 evaluate heat transfer performance on the effusion wall cold side. The geometries present equal numbers of
375 impingement and effusion holes, both arranged in staggered configurations, but with sparser (Geometry 1)
376 and denser (Geometry 2) arrays. The effect of two different impingement and effusion holes relative position-
377 ing is investigated (Assembly 1 and 2), as well as of Reynolds number and coolant extraction.

378 For the sparser geometry, results present strong primary heat transfer peaks and small jet-jet interactions.
379 For this configuration, the presence of effusion holes causes area averaged Nu to increase up to 30% with
380 respect to impingement only case, without significant differences in heat transfer pattern shape. This has
381 been interpreted as the effect of a reduction of spent coolant flow recirculation. A significant effect of holes
382 assembly is also present: by shifting the hole arrays of $\frac{1}{2}$ holes pitch (i.e. changing the assembly), reductions
383 up to 33% in area averaged Nu are recorded (at around $Re_j = 2500$), since impingement jets are deflected
384 by the effusion holes flow field. The differences between the two assemblies tend to decrease as Re_j increases.
385 For Geometry 2, heat transfer is strongly influenced by the presence and assembly of extraction holes. Assem-
386 bly 1 presents a characteristic heat transfer pattern, with clover-shaped primary peaks under the impingement
387 holes and strong secondary peaks midway between the jets induced by the fountain effect. Assembly 2 shows
388 primary peaks elongated towards the two nearest effusion holes and with a higher magnitude with respect
389 to Assembly 1, but also large regions of low and unstable heat transfer: this results in an area averaged Nu
390 values similar to the ones of Assembly 1, but also in a less homogeneous distribution. For both assemblies,
391 significant heat transfer increases are recorded (around 15%) with respect to impingement only configuration,
392 due to the suppression of the intense crossflow generated by the dense hole array.

393 The results of this study have shown that different geometries provide characteristic results: slight modifica-
394 tions to the cooling system can significantly alter both shape and magnitude of heat transfer pattern, due to
395 the wide range of parameters on which heat transfer depends and the strong interactions between different
396 phenomena. Despite the small number of investigated geometries, some overall considerations can be derived
397 for impingement/effusion systems design:

- 398 • For sparse geometries (X/D_i around 10, as Geometry 1) care should be taken so that the internal
399 pressure field does not deflect the jets, thus maximizing heat transfer.
- 400 • For dense geometries (X/D_i around 3, as Geometry 2) overall cooling performances show low sensibility
401 to the impingement/effusion holes relative position (at least in the investigated configurations): as a
402 consequence, effort should be made to avoid the occurrence of low heat transfer regions, which can be
403 obtained by making jets impinge in the region farthest from the effusion holes.

- CFD is a suitable tool for predicting fluid and thermal phenomena involved by an impingement/effusion system: steady RANS simulations provide reliable area averaged heat transfer values in the range of $\pm 10\%$, but only unsteady simulations are able to retrieve the correct shape and entity of time averaged Nusselt number distribution.

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