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This is a Final draft version of a publication
published by Elsevier
in Nuclear Engineering and Design

DOI: 10.1016/j.nucengdes.2019.05.011

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Please cite the publication as follows:

Gallego-Marcos, I., Kudinov, P., Villanueva, W., Puustinen, M., Räsänen, A., Tielinen, K., Kotro, E. (2019). Effective momentum induced by steam condensation in the oscillatory bubble regime. Nuclear Engineering and Design, vol. 350. pp. 259-274. DOI: 10.1016/j.nucengdes.2019.05.011

**This is a parallel published version of an original publication.
This version can differ from the original published article.**

Effective momentum induced by steam condensation in the oscillatory bubble regime

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ABSTRACT

The spargers used in Boiling Water Reactors (BWR) discharge steam from the primary coolant system into a pool of water. Direct steam condensation in subcooled water creates sources of heat and momentum determined by the condensation regimes, called “effective sources” in this work. Competition between the effective sources can result in thermally stratification or mixing of the pool. Thermal stratification is a safety concern in BWRs since it reduces the steam condensation and pressure suppression capacity of the pool. In this work, we present semi-empirical correlations to predict the effective momentum induced by steam condensation in the oscillatory bubble regime, relevant for the operation of spargers in BWRs. A Separate Effect Facility (SEF) was designed and built at LUT, Finland, in order to provide the necessary data. An empirical correlation for the effective momentum as a function of the Jakob number is proposed. The Kelvin Impulse theory was also applied to estimate the effective momentum based on information about the bubble dynamics. To do this, new correlations for the bubble collapse frequencies, maximum bubble radius, velocities, pressure gradient and heat transfer coefficient are proposed and compared to available data from the literature. The effective momentum induced by sonic steam jets appears to be constant in a wide range of studied Jakob number. However, further experimental data is necessary at larger Jakob numbers and steam mass fluxes.

KEYWORDS

Effective momentum; Kelvin Impulse; bubble radius; collapsing frequency; heat transfer coefficient.

HIGHLIGHTS

- Equal momentum rate at injection holes and condensate liquid in oscillatory regimes
- Effective momentum is strongly dependent on the Jakob number in sub-sonic regimes
- Sonic experiments suggest a weaker dependency on the Jakob number
- Collapsing frequency is larger than the bubble life frequency at low subcoolings

NOMENCLATURE

Latin	
a	Sonic velocity
A	Area
c_p	Specific isobaric heat capacity
d	Diameter
f	Frequency
F	Force
g	Gravitational acceleration

G	Steam mass flux
h	Heat transfer coefficient, enthalpy
Ja	Jakob number, $c_{pL}\Delta T/h_{fg}$
$Ja^\dagger$	Jakob number with density ratio $\rho_L c_{pL}\Delta T/\rho_s h_{fg}$
K	Jet expansion coefficient
l_s	Submergence depth
M	Momentum source (rate), equivalent to F ; Mach number, U_s/a_s
n	Number of holes
P	Pressure
Re	Reynolds number, $\rho_s U_s d/\mu_s$
Q	Volumetric flow rate
r	Radial coordinate
R	Radius
St	Strouhal number, $fd\rho_L/\rho_s U_s$
t	Time
T	Temperature
U	Mean flow velocity
V	Volume
Wa	Non-dimensional number for effect of air on collapse frequency
We	Weber number, $\rho_s U_s^2 d/\sigma$
w	Mass fraction
x	Axial coordinate
Greek	
γ	Heat capacity ratio
ΔT	Subcooling, $T_{sat} - T_L$
ρ	Density
λ	Thermal conductivity
σ	Surface tension
Sub and super indices	
$*$	Maximum
a	Air
b	Bubble
bl	Bubble life
c	Collapse
d	Detachment
e	Ellipsoid
eff	Effective
fg	Phase change
i	Injection hole
KI	Kelvin Impulse
L	Liquid
s	Steam

<i>sat</i>	Saturation
<i>sup</i>	Superheat
<i>th</i>	Theoretical
∞	conditions in the pool at the depth of steam injection holes
Acronyms	
AP	Advanced Pressurized
BWR	Boiling Water Reactor
CFD	Computational Fluid Dynamics
EHS	Effective Heat Source
EMS	Effective Momentum Source
MPE	Mean Percentage Error $\frac{100}{N} \sum_{n=1}^N (x_{exp} - x)/x_{exp} $
PC	Poly-Carbonate
SEF	Separate Effect Facility
TC	ThermoCouple

INTRODUCTION

Boiling Water Reactors (BWR) and Advanced Pressurized (AP) reactors are designed with large water pools where steam is injected during normal operation and accidental conditions [1]. For certain steam injection conditions, the competition between momentum and buoyancy forces created by the steam injection can result in thermal stratification, leading to higher containment pressures than in completely mixed pool conditions.

Prediction of direct contact condensation and its effect on a large pool is a challenge for contemporary simulations due to the need to resolve very large (pool) and very small (steam-water interface) scales of time and length during long transients. To enable the prediction of such transients, Li & Kudinov (2010) [2] introduced the concepts of the Effective Heat Source (EHS) and Effective Momentum Source (EMS) models. The aim of these models is to establish a connection between the steam injection conditions and resulting heat and momentum sources induced by the steam condensation in different regimes. These sources can be used as boundary conditions in any CFD-type code to predict the effect of a steam injection (such as large scale pool circulation or stratification) using a single-phase liquid solver. This approach improves the computational efficiency compared to modelling of two-phase flow, which would be unaffordable for a long-term transient in a large-scale pool. Work done by the authors in [3] demonstrated that the chugging regime produces a time-averaged momentum that can be predicted if the frequency and amplitude of the liquid level oscillations inside the pipe are known. The EMS model for chugging was implemented in GOTHIC and validated against large-scale pool experiments performed in the PPOOLEX facility [4, 5, 6, 7].

The goal of this work is to develop empirical correlations that can be used in EHS/EMS models for the oscillatory bubble regime. This regime is relevant for BWR plant conditions when spargers inject steam at mass fluxes between 75-300 kg/(m²s) [8, 9] through a large number of relatively small holes. A set of experiments was carried out in the PPOOLEX and PANDA facilities to analyze the pool behaviour induced by this regime [10]. Simulation of the pool transients using the CFD code of ANSYS Fluent was later done in [11] to infer the effective heat and momentum sources, which could not be directly measured in the experiments. Parametric studies done in [11] showed that the uncertainty in other injection parameters specific to the sparger (such as the injection angle, azimuthal profile of the momentum, and induced turbulence source) could affect the estimated momentum sources.

The oscillatory bubble regime was studied experimentally by Fukuda [12], who provided correlations for the collapsing frequency, maximum bubble radius and heat transfer coefficient. Further analysis was carried out by Simpson et al. [13], who proposed some changes in the correlations given by Fukuda. Analysis of the collapsing frequencies induced by multi-hole spargers was done by Cho et al. [14], who compared their experimental results to the available correlations and proposed possible improvements. Collapsing frequencies of sonic jets and oscillatory bubble are also presented in [15]. Analytical and experimental observations of the bubble behavior was also done by Yuan et al. [16] based on the Rayleigh-Plesset equation and force balances. The effect of non-condensable gases in the steam injection was investigated by [17], who observed a significant decrease of the collapsing frequencies at air mass fractions of 1 %.

The previous works analyzed the oscillatory bubble regime for the range of steam mass fluxes and pipe diameters relevant to spargers in the BWRs. Other works not focused on plant conditions are the ones from Tang et al. [18], who analyzed the bubble surface effects at fluxes of about 15 kg/(m²s). Another set of important works are those related to cavitation bubbles and the theory of the Kelvin Impulse introduced by Benjamin and Ellis [19] and Blake [20] and further developed by Supponen et al. [21, 22]. This theory provides a theoretical background for the analysis of momentum created by a bubble collapse.

Measurements of the liquid momentum induced by steam condensation have not been addressed in any previous works. In order to obtain data on the liquid momentum, a Separate Effect Facility (SEF) was built in Lappeenranta University of Technology (LUT), Finland. Based on the SEF data correlations are proposed for the EMS in Section 3. The Kelvin Impulse theory is used to propose another approach for the estimation of the EMS, based on the bubble dynamics, Section 4. To do this, new correlations for the collapsing frequencies, maximum bubble radius, pressure gradient, velocities, and heat transfer coefficient are proposed and compared to available data from the literature. The analysis provides some valuable insights in the relation between the EMS and bubble dynamics phenomena.

1. EMS FOR THE OSCILLATORY BUBBLE REGIME

The EMS induced by a steam injection into a pool is defined in [3] as

$$M_{eff}(t) = \frac{1}{\Delta t} \int_{t-\Delta t}^t M(\tau) d\tau \quad (1)$$

where the integral represent the time-average of the instantaneous variations of the sources over a period of time Δt . For example, in the oscillatory bubble regime, Δt should be taken much larger than the bubble life time during its growth, detachment and collapse. The goal is to find $M_{eff}(t)$ as a function of parameters determining steam condensation.

The first approach is based on establishing a correlation between the measured liquid momentum and the steam injection conditions (Section 3). The second is based on the Kelvin Impulse, where the time-dependent bubble volume and pressure gradient across the bubble is integrated over a complete growth-collapse cycle (Section 4).

2. SEF FACILITY

A over-view of the SEF facility is presented in Figure 1. The primary goal of the facility is to measure the momentum rate generated by the steam injection through sparger holes and the liquid momentum rate downstream the condensation. This is achieved by measuring (i) the reactive force at the sparger pipe, and (ii) the reactive force at the water momentum catcher, which is composed by the PolyCarbonate (PC) pipe and the disk stack. The momentum catcher is designed such that all axial momentum rate of the liquid is transferred into the force acting on the catcher. The forces were measured using a LUX-B linear compression load cell with a rated capacity of ± 50 N.

Steam is generated using a 1.0 MW heater and injected into the pool through a perforated plate located at the end of the sparger pipe, which is insulated to minimize steam condensation inside it. The steam is condensed in a sufficiently large water tank of dimensions 1500×300×600 mm.

Most of the tests (except S8 and S9) were done with configuration shown in Figure 1, where condensed flow is guided through the PC pipe to impinge on the disk stack. The support rods of the sparger and PC pipes are allowed to rotate around their axis. Therefore, the reactive forces (EMS) can be measured using two independent force sensors connected to rigid supporting structures fixed to the ground (see Figure 1 and Figure 2). The disk stack was designed to convert axial motion of the liquid into radial outward flow. Pre-test simulations performed with ANSYS Fluent (not presented in the paper) showed that stacking several disks with gradually reducing inner diameter can help to avoid a backflow in the PC pipe, which could affect the measurements. Axial distance between the disks was 5 mm.

Note that another configuration was used in the SEF facility for tests S8 and S9 (Table 1) where a 300 mm long PC pipe was attached to the cylinder surrounding the injection holes. The section of the PC pipe closest to the injection holes was drilled with a ring of $6 \times \phi 25$ mm holes. Between this ring and the PC pipe outlet, another $30 \times \phi 25$ mm holes were drilled along the lower half of the PC pipe. The upper section of the PC pipe was not drilled to prevent any buoyant flow from escaping through them. The drilled holes allowed liquid to be entrained into the condensation region and to equalize the pressure between the inside and outside of the PC pipe. Thus, the momentum rate of the liquid could be measured by the sparger pipe force sensor.

One disadvantage of the drilled PC pipe configuration is that it only provides one measurement of momentum rate. Comparison between the independent measurements of the forces induced on the sparger pipe and momentum catcher was consider necessary to assess possible changes of the momentum rate in the process of the oscillatory bubble collapse (see Figure 7 and Figure 8). For this reason, the configuration of PC pipe with disk stack presented in Figure 1 was used for the rest of the experiments.

The condensation of the steam bubbles was recorded using a Phantom MIRO M310 camera, at 2800 frames per second (fps). Some of the first tests were recorded at 700 fps. A backlight was located behind the pool to increase the contrast of the recorded bubbles. The camera was positioned at an axial distance of about 200 mm from the injection holes. This position was needed to cover the complete length travelled by the bubbles, which becomes larger as the pool temperature increases (Figure 5 and Figure 6). With this arrangement, bubbles close to the injection holes could not be recorded with a sharp frontal view, which could induce over-predictions of their size in the image analysis. Unfortunately, the precise location of the camera was not kept for each experiments. Thus, a small over-prediction can be expected at the initial stages of the time-dependent radius presented in Section 4.2.

The frequencies of bubble collapse were measured using a 7000 Hz pressure transducer located 80 mm below the injection holes. The same 7000 Hz frequency was used in the force measurements. The steam properties inside the sparger were measured with a K-Type TC and a pressure transducer located about 150 mm near the injection holes. In the pool three K-type TCs and a level meter (based on differential pressure between gas space and pool bottom) were used to measure the pool bulk temperature and liquid level. These values were used to determine the hydrostatic head at the level of the injection holes.

Figure 1: Separate Effect Facility (SEF). Over-view and details of the injection holes and disk stack. Line (—) denotes the cross section plane used to visualize the interior of the pool.

Figure 2: Installation of the force sensor and its connection to the sparger pipe.

The force measurement at the sparger was calibrated applying forces at the injection holes using a Sauter force gauge. The applied force was varied stepwise, from 5.5 to 50 N. Assuming a normal distribution, the 95 % confidence interval for the sparger force error was +2.2 % and -0.4 %. This results show that a small fraction of the applied force is absorbed by the sparger pipe and other structures.

The error of the force measurement at the PC pipe was assessed by injecting water through the sparger pipe into the pool. The mass flow was increased and decreased stepwise covering a range from 1.5 to 35 N. Due to jet momentum conservation, this setup should have led to the same force measurement at the sparger and PC pipe. As we can see in Figure 3, the forces at the PC pipe were generally larger than at the sparger. Since the sparger and PC pipe structures are similar, the over-estimation could be attributed to partial rebounds of the axial flow at the disk stack, instead of being deflected radially outwards. Assuming a normal distribution, the 95 % confidence interval for the PC pipe force errors were determined to be +8.8 % and -2.5 %.

Figure 3: Forces measured at the sparger and PC pipe during the calibration test of water injection into a water pool. The negative error spikes at the beginning of each step are due to the time it takes for the sparger jets to reach the PC pipe.

2.1. Experimental procedure

The experimental campaign was divided into two sets (Table 1). First, experiments were run without PC pipe to obtain data on the momentum induced by steam condensation without flow disturbances. After this, the PC pipe was added to determine if the liquid momentum rate after the condensation can be different from the one measured at the steam injection sparger. The steam mass flux was maintained constant in each experiment, and high-speed data (pressure, video and forces) were recorded during 5-6 intervals of 12.5 s as the pool temperature gradually increased. The steam and liquid temperatures during the high-speed data recordings are presented in Figure 4.

In the data post processing we studied the effects of non-dimensional numbers such as the Jakob number (ratio of sensible to latent heat), Reynolds number (ratio of inertia to viscous forces), Weber number (ratio of inertia to surface tension forces), and Mach number (ratio of steam to sonic velocities). The parameters used to define the numbers are given in the nomenclature. The values covered in these experiments are presented in Table 1. Note that correlations obtained in this paper should be used with great caution outside these ranges.

Table 1: Test matrix used in the SEF experiments. Steam mass flux G , Reynolds Re , Weber We and Mach M numbers are the averaged values for each experiment. The Jakob Ja number is presented with the min-max values reflecting temperature change in the pool. All variables are computed assuming steam properties at hydrostatic pressure P_∞ . Shakedown tests S1 and S2 are not included in this paper.

Exp. identifier	# injection holes × diameter	G [kg/(m ² s)]	Ja [-]	$Re/10^3$ [-]	$We/10^3$ [-]	M [-]
No PC pipe						
S10	1×16 mm	80	0.025 – 0.108	105	2.60	0.45
S6		129	0.028 – 0.113	168	6.64	0.63
S11		176	0.025 – 0.109	229	12.53	0.80
S7		213	0.018 – 0.105	277	18.38	0.90
S5		320	0.060 – 0.124	408	35.3	1.12
S12	1×12 mm	123	0.032 – 0.110	120	4.52	0.58
S13		170	0.029 – 0.110	166	8.71	0.77
S3	3×8 mm	123	0.039 – 0.125	139	5.18	0.52
S4	pitch = 26 mm	330	0.032 – 0.125	365	34.23	1.10
PC pipe (with disk stack)						
S14	1×16 mm	70	0.044 – 0.126	91	1.89	0.35
S15		123	0.022 – 0.111	160	6.07	0.58
S18		174	0.041 – 0.127	227	11.75	0.75
S19		323	0.053 – 0.136	413	33.48	1.17
S16	1×12 mm	125	0.042 – 0.112	122	4.63	0.61
S17	2×8 mm pitch = 36 mm	125	0.043 – 0.112	115	4.31	0.56
PC pipe (with drilled holes)						
S8	1×16 mm	122	0.032 – 0.108	159	5.94	0.60
S9		317	0.039 – 0.112	405	33.70	1.17
Ranges	8-16 mm	70-330	0.025 – 0.136	91-413	1.89-33.48	0.35-1.17

Figure 4: Steam temperatures inside the sparger pipe and pool liquid temperatures, as a function of the steam mass flux. Steam and liquid temperatures are given for all the steps where force data was recorded within an experiment. For example, in the S14, the first force measurement was taken at a $T_{\text{pool}} = 23$ °C and a $T_{\text{steam}} = 102$ °. Further force measurements were taken as the pool temperature increased.

2.2. Image processing

Post-processing of the video images was done with Matlab. The steps are outlined in Figure 5. First, a Wiener filter was applied to remove high frequency noise using the local image mean and variance at pixel groups of 3×3 . Then, a sharpening filter was applied to increase the contrast. A total of 15 seed points were manually defined to generate a contour along the bubble surface, composed of lines perpendicular to the seed point connections. An automatic detection of the surface was based on the maximum color gradients within each of the perpendicular to the surface lines. Finally, the bubble was approximated by an ellipse through a least-square function, and the data of the ellipse axis and centroid was saved to a text file. Axis-symmetry was assumed around the ellipse axis aligned with the flow direction.

Approximation to an ellipse was necessary due to the uncertainty of the bubble surface at the region close to the injection holes, which prevented us from obtaining a closed surface boundary. Analysis of the video images showed that the bubble shape is indeed similar to an ellipse (Figure 6). Thus, the error of the ellipse approximation is not expected to be larger than the axis-symmetry assumption, which would have to be done unless several cameras were placed at different angles.

(b)

Figure 5: Image processing (a) steps and (b) detail of the detected bubble boundary. Images contain the (●) user definer points, (●) detected bubble boundary and (—) approximated ellipse.

3. EMS CORRELATIONS BASED ON STEAM INJECTION CONDITIONS

In this section we present empirical correlation for the EMS based on the steam injection conditions.

3.1. Qualitative analysis of the video images

The oscillatory motion presented in Figure 6 is similar to what was previously reported by Simpson et al. [13], Fukuda [12] and Yuan et al. [16]. That is, the bubble begins to grow attached to the injection holes, detaches when the force balance becomes positive in the direction of the steam injection, and collapses as the neck connecting the bubble to the injection hole reduces the steam flow into the bubble.

Yuan et al. [16] implicitly assumed that the collapse frequency f_c is equal to the frequency of the sinusoidal evolution of the bubble radius, which we will name bubble-life frequency f_{bl} . This is not necessarily the case. At low subcoolings, detached bubbles can move a large enough distance from the injection holes before collapsing (Figure 6). During this period of time a new bubble can start to form (i.e. $f_c > f_{bl}$). The implicit assumption of $f_c = f_{bl}$ done by Yuan et al. resulted in an apparent deviation between theory, which estimated f_{bl} , and experiment where f_c was measured. Further quantitative analysis of the f_c/f_{bl} ratio is presented in Section 4.2.

(a)

(b)

Figure 6: Video frames of the (a) S10 experiment, $G = 80 \text{ kg}/(\text{m}^2\text{s})$ and (b) S11 experiment $G = 176 \text{ kg}/(\text{m}^2\text{s})$. Injection hole diameter $d = 16 \text{ mm}$.

3.2. Force measurements

The forces measured during the oscillatory bubble and sonic jet regimes are presented in Figure 7 and Figure 8, respectively. The oscillating forces suggest that steam periodically accelerates and decelerates at the injection hole as a result of the bubble motions. In the sub-sonic regimes, the negative forces are attributed to possible back-flow of steam towards the injection hole during the detaching/necking phase or to wake effects caused by the previous bubble. The forces measured in the sonic regimes were always above zero. This observation is in agreement with the video images, which show that in this regimes a steam jet is always attached to the injection holes, and only a fraction of its downstream volume can detach and lead to the oscillations observed in Figure 8. Another important observation is that in the oscillatory bubble regime, time-averaged forces were growing along with the pool temperature (Figure 7b). In sonic jets, the measured forces were much less sensitive to the pool temperature (Figure 8b).

Some of the time-averaged forces were observed to lay within the region of uncertainty of the PC pipe force estimated in the calibration test (Figure 3). This suggests that bubble momentum after detachment was efficiently transferred to the liquid, resulting in a similar force at the sparger and PC pipe. On the other hand, some points were observed to lay outside of the uncertain region, especially in the sub-sonic experiments. This could indicate that the oscillatory regimes induce larger momentum to the liquid during the bubble collapse. However, it could also be that the steam injection test led to a larger amount rebounds of the axial flow at the disk stack than in the calibration test (performed injecting water). Due to the small deviation from the uncertainty region and to the similar behavior as in the calibration test (force in PC pipe > force in sparger) we will assume that the deviations between sparger and PC pipe measurements can be within the region of uncertainty of the steam injection experiments. Thus, the EMS was taken as the one measured at the sparger pipe.

Figure 7: Forces measured in the SEF-S16 experiment: 1×12 mm, $G = 125$ kg/(m²s). (a) Time-dependent forces, and (b) mean values during a 12.5 s frame compared with the theoretical estimate M_{th} given by equation (3).

Figure 8: Forces measured in the SEF-S19 experiment: 1×16 mm, $G = 325$ kg/(m²s). (a) Time-dependent forces, and (b) mean values during a 12.5 s frame compared with the theoretical estimate M_{th} given by equation (3).

3.3. Effective momentum source

The EMS for the oscillatory bubble regime was defined in [11] as a function of the steam injection parameters and the non-dimensional coefficient C given by

$$C = \frac{M_{eff}}{M_{th}} \quad (2)$$

where M_{eff} is the effective momentum measured in the experiments and M_{th} is a theoretical steam momentum given by

$$M_{th} = \rho_s A_i U_s^2 + A_i (P_s - P_\infty) \quad (3)$$

The terms in M_{th} are as defined as follows, $\dot{m}_s$ (or steam velocity U_s) is the averaged steam flow rate injected into the pool, A_i is the total injection hole area, ρ_s is the steam density, and P_s the steam pressure at the injection holes. The pressure P_s is assumed to be equal to the hydrostatic pressure of the pool P_∞ in sub-sonic regimes, and dependent on the heat capacity ratio γ and upstream pressure P_0 in sonic regimes as

$$\frac{P_s}{P_0} = \left(\frac{2}{\gamma + 1} \right)^{\gamma/(\gamma-1)} \quad (4)$$

It should be noted that equation (4) is valid for isentropic flows. The sudden flow contraction at the injection hole could cause small deviations in the prediction.

As we can see in Figure 7b and Figure 8b, the M_{th} predicted by equation (3) captures the order of magnitude of M_{eff} , but still requires a correction through the C coefficient to consider the effect of condensation regimes.

The sub-sonic M_{eff} and C coefficients were observed to have a stronger dependency on the Jakob number than in the case of sonic jets (Figure 9). Therefore, separate correlations for C were proposed for each regime. The transition between these regimes could not be determined due to the lack of data in the $M \sim 1$ region.

Figure 9: EMS M_{eff} and condensation regime coefficient C measured in SEF as a function of the Jakob number. Sonic data points are connected with dotted lines.

A preliminary non-linear least-squares fitting of the sub-sonic C coefficients as a function of Ja , Re and We resulted in near zero exponents for the Re and We numbers, suggesting a negligible effect of the steam mass flux and injection hole diameter. A new fitting based on Ja number alone resulted in equation (5), which showed good agreement with experimental data in Figure 10.

$$C = 0.48 \cdot Ja^{-1/3} \quad (5)$$

The data points inside the dashed rectangle “A” in Figure 10 were observed to present some deviations from the rest. As we can see in Figure 9, these points were obtained at Ja values larger than 0.12. The sonic jet experiments S4 and S5 also indicated decreasing values of M_{eff} and C at similar Ja values. This suggests a regime transition, difficult to assess with the current data set. Further experiments at larger Ja values (pool temperatures below 25 °C), should be performed to clarify this. If necessary, a separate correlation should be provided for the regimes at large Ja .

Figure 10: Comparison between the sub-sonic EMS M_{eff} and condensation regime coefficient C measured in SEF to the ones one predicted by equations (2)-(5).

The definition of the Ja number used in equation (5) is the original one proposed by Max Jakob (ratio of sensible to latent heat), which was also used by Fukuda [12] for the analysis of the oscillatory bubble regime. One would expect that this definition is applicable for near saturated steam conditions, which is also the case in the SEF experiments. For significant steam super-heat one might need to use

$$Ja_{sup} = \frac{c_{ps}(T_{sup} - T_{sat}) + c_{pL}(T_{sat} - T_L)}{h_{fg}} \quad (6)$$

where the numerator is the specific sensible heat during the steam and liquid cooling. However, further data would be needed to confirm this. Other definitions of the Ja number include a multiplier of the density ratio [13, 14], equation (7).

$$Ja^\dagger = \frac{\rho_L}{\rho_S} \left(\frac{c_{pL}(T_{sat} - T_L)}{h_{fg}} \right) \quad (7)$$

However, the density ratio does not necessarily need to have the same exponent as the original Ja number and should be considered as a separate non-dimensional factor. Since the density ratio was almost constant in the SEF experiments (similar hydro-static head at injection holes, which leads to a similar saturation density), the exponent of the density ratio could not be determined from the dataset.

Fitting of C as a function of the dimensional injection parameters led to a similar (but simplified) dependency as equation (5), dominated by the subcooling $\Delta T = T_{sat} - T_L$.

$$C = 4.28 \cdot \Delta T^{-0.35} \quad (8)$$

The small variation of the exponent between equations (5) and (8) is due to the variability of the water properties not considered in equation (8). Comparison between the experimental data and equation (8) is not presented since it led to almost the same behavior as in Figure 10, with a similar Mean Percentage Error (MPE) of 15.9 %. It should be noted that in equation (8) the constant 4.28 is not dimensionless. This is applicable to the other dimensional correlations presented further in this paper.

The C coefficients for sonic jet regimes would require more data for calibration since all test performed at $M > 1$ were run at similar steam mass fluxes of $\sim 320 \text{ kg}/(\text{m}^2\text{s})$. Neglecting the steam mass flux, a fitting of C using other dimensional injection conditions led to $C \propto \Delta T^{-0.04} d^{0.01}$, showing almost no dependency on these variables. However, the decrease of M_{eff} and C at high Ja numbers should be addressed further (Figure 9). For the analyzed range we can only conclude that the C has a quasi-constant value given by

$$C_{G=320 \text{ kg}/(\text{m}^2\text{s})} = 0.84 \quad (9)$$

Further experiments are needed to address the variability at higher steam mass fluxes and Ja numbers.

4. EMS CORRELATIONS BASED ON BUBBLE COLLAPSE IMPULSE

In this section we present the second correlation for the EMS, based on the theory of the Kelvin Impulse. Correlations for the bubble parameters such as frequency, radius, velocity, pressure gradient and heat transfer coefficient are also presented and compared to other available data from the literature.

The growth and collapse of a bubble induces an impulse I to its surrounding liquid given by [22]

$$I = \int_0^{t_{bl}} \nabla P V dt \quad (10)$$

where ∇P is the pressure gradient across the bubble and V its volume. The Kelvin Impulse states that the liquid momentum must be preserved in the liquid upon complete bubble collapse. For this to occur, the bubble cannot remain perfectly spherical since this would cause a cancellation of all the momentum vectors. The bubble has to deform and allow liquid to pierce through its surface to carry the momentum given by equation (10).

The degree of deformation is determined by the pressure gradient across the bubble, which can be induced by proximity to walls, velocity gradients in the flow, etc. Even when 3 mm bubbles are placed in a quiescent liquid and far from the walls, the static head is enough to induce a pressure gradient which would cause the deformation [22].

Based on the previous discussion, the EMS induced by the oscillatory bubble regime can be written as

$$M_{eff} = n f_c \int_0^{t_{bl}} \nabla P \left(\frac{4}{3} \pi R^3 \right) dt \quad (11)$$

where f_c is the frequency of collapse, n the number of injection holes (implicit assumption of no changes in the momentum due to interaction between injection holes), and R the sphere equivalent bubble radius. Sections 4.1-4.3 will be dedicated to the analysis of the SEF experiments to find closure correlations for equation (11).

It should be noted that the collapse phase at large subcoolings of some steam bubbles led to a steam-water cloud with no clear boundary between vapor and liquid (Figure 6). This behavior is not captured in equations (10) or (11), where it is assumed that the gas-liquid interface is a well-defined boundary.

4.1. Collapsing frequency

The collapsing frequencies measured in SEF are presented in Figure 11. The quasi-linear dependency with the subcooling is similar to what was observed in the experiments from Table 2. In the low steam mass flux experiments (S10 and S14), the dependency with the subcooling was inverted at about 60 °C. A similar behavior can be observed in [17], where experiments performed at 70-100 kg/(m²s) settled at an almost constant frequency at subcoolings above 70 °C.

Figure 11: Collapsing frequencies measured during the SEF experiments. Experiments with multi-hole injection and drilled PC pipe not included.

Correlations for the collapsing frequency have been proposed by Fukuda [12] as

$$f_c = 0.06 \frac{\Delta T}{d} \quad (12)$$

who observed no dependency with steam mass flux G . Simpson et al. [13] provided a non-dimensional correlation based on the Strouhal St , Jakob and Reynolds numbers which was further developed by Damasio et al. [23] by adding

the Weber number. Cho et al. [14] extended Damasio et al. correlation for multi-hole spargers by re-calibrating some of the coefficients and adding a new factor containing the pitch to diameter ratio $I_\phi = P/d$

$$St = k_3 (Ja^\dagger)^{a_3} (Re)^{b_3} (We)^{c_3} (I_\phi)^{d_3} \rightarrow f_c \propto \frac{\Delta T^{1.09} G^{0.24}}{d^{0.94}} \quad (13)$$

where the coefficients are $k_3 = 0.00174$, $a_3 = 1.093$, $b_3 = 0.891$, $c_3 = -0.827$ and $d_3 = 0.298$. Equation (13) should only be used in the range of P/d ratios analyzed by Cho et al [14]. The current definition of I_ϕ would predict larger frequencies as the pitch increases. However, large enough pitches should result in a negligible interaction between jets, for which $I_\phi \approx 1$ would be more appropriate. Further data is required to provide a new definition of I_ϕ . For the case of single-hole injection, we assumed $I_\phi = 1$.

Table 2: Available experimental data on the collapsing frequency of oscillatory bubbles. All experiments are horizontal single-hole injection unless noted different.

	G [kg/m ² s]	d [mm]	T_L [°C]	l_s [m]
Fukuda [12] ^a	80-233	8, 16, 27	25-90	0.12
Simpson et al. [13] ^a	147-305	6, 22	25-65	0.23
Cho et al. [14] ^b	70-140	5	30-85	0.36
Hong et al. [15]	200-250	10	35-95	1.1
Yuan et al. [16]	250-300	8, 10	15-60	0.5
Li et al. [17]	70-250	20, 32	25-60	0.5
SEF	70-330	8, 12, 16	20-85	0.25

^a Vertical downwards injection

^b Multi-hole sparger

Application of equations (12) and (13) to the experiments from Table 2 show a significant spread (Figure 13). According to Li et al. [17] the collapsing frequencies are reduced significantly when steam is injected with air mass fractions of 1 %. The presence of air could be a possibility for this deviation. However, it is not possible to determine which experiments from the literature could have been affected by this. Li et al. [17] proposed a coefficient $w_a^{-0.26}$ to be added to an equation similar to (13) to account for effects of air. This coefficient tends to infinity at pure steam conditions of $w_a = 0$. Thus, based on Li et al. data, we propose a new correlation, equation (14), which is compared to experimental data in Figure 12.

$$Wa = \frac{f_{c,air}}{f_c} = \frac{1}{1 + 285 \left(\frac{w_a}{G} \right)^{0.62}} \quad (14)$$

Figure 12: Comparison between the $f_{c,air}/f_c$ ratio measured by Li et al. [17] to the one predicted by equation (14).

The frequency correlation proposed in this work is based on the original definition of the Jakob number (Ja). The density ratio was not included since this one is already present in the Strouhal number. Sensitivity studies showed that adding the density ratio as a separate factor did not improve the prediction or agreement between experiments. A preliminary non-linear least-squares fit of all available experimental data showed a spread similar to the one obtained with equation (13). The data from Fukuda was observed to have a different orientation than the rest. The reasons for this deviation are not clear and should be further analyzed. They could be due to physical phenomena not captured in the correlations or to uncertainty in Fukuda's measurements. A new fitting was performed excluding Fukuda's data, which led to equation (15)

$$St = k_4 (Ja)^{a_4} (Re)^{b_4} (We)^{c_4} (I_0)^{d_4} \rightarrow f_c \propto \frac{\Delta T^{0.78} G^{0.36}}{d^{0.60}} \quad (15)$$

where the coefficients are $k_4 = 0.033$, $a_4 = 0.78$, $b_4 = 1.44$, $c_4 = -1.04$, and $d_4 = 0.298$. The main drawback of Cho et al [14] correlation is that it was obtained using 5 mm holes only. This prevented them from calibrating the effect of the diameter adequately, leading to significant under-estimations for larger diameter experiments. The correlation proposed in equation (15) improves this prediction.

The Wa factor given by equation (14) could be added as a multiplier to equation (15) to account for presence of non-condensable gases. However, more data might be required to further calibrate this term.

Figure 13: Comparison between the collapsing frequencies obtained from the literature and measured in SEF with the correlations given by Fukuda [12], Cho et al. [14], and the new correlation proposed in equation (15). MPE is computed excluding Fukuda's data.

4.2. Bubble radius

The bubble radius presented in Figure 14 was obtained through analysis of video images such as the ones presented in Figure 6. The last stages could not be analyzed since it was not possible to determine the bubble size from the steam-water cloud generated by the collapse. The time of bubble detachment t_d was estimated with

$$\frac{x - a}{d} > 1 \quad (16)$$

where a is the ellipse's semi-axis aligned with the flow direction, x the distance travelled by the ellipse center and d the injection hole diameter. The locations of detachment presented in Figure 14 show a good qualitative agreement with the ones observed by Yuan et al. [16].

Figure 14: Evolution of the bubble radius as a function of the subcooling and steam mass flux. Mean values and error bars are calculated based on the analysis of 9 independent bubble transients. Red markers correspond to the detachment time predicted by equation (16).

Yuan et al [16] applied the Rayleigh-Plesset (RP) equation to the oscillatory bubble regime and obtained that the bubble radius behaves as a damped harmonic oscillator of the form

$$R = R^*(1 + Ae^{-\beta t} \sin(\omega t + \varphi)) \quad (17)$$

where R^* is the maximum bubble radius, A a coefficient and β the damping factor. The A and β coefficients were calibrated in [16] for the stages before and after bubble detachment during an injection at $G = 300 \text{ kg/(m}^2\text{s)}$ and $T_L = 35 \text{ }^\circ\text{C}$. Coefficients for the collapse stage, where R should become zero, were not provided.

Estimation of the Kelvin Impulse using equation (11) requires that the correlation for the time-dependent radius R reaches zero upon complete collapse. In this work, the A and β coefficients could not be calibrated for the last stages of the collapse phase due to the large uncertainty of the bubble radius as a result of the steam-water cloud formed during the collapse (see Figure 5). For this reason, equation (17) was simplified to a harmonic oscillator

$$R = R^* \sin(\pi f_{bl} t) \quad (18)$$

valid for $t < 1/f_{bl}$, where f_{bl} is the bubble-life frequency associated with the time-dependent radius (Section 3.1). The frequency f_{bl} was estimated by performing a non-linear least-squares fitting of the time-dependent bubble radius. Once obtained, the f_c/f_{bl} ratio was observed to be mainly correlated with the subcooling, reaching a value of unity at about $70 \text{ }^\circ\text{C}$ (Figure 15).

Fitting of the f_c/f_{bl} ratio as a function of the non-dimensional numbers led to

$$\frac{f_c}{f_{bl}} = 2.1 \cdot Ja^{-0.32} Re^{-0.19} We^{0.11} \quad (19)$$

Comparison between the prediction from equation (19) and experimental data is shown in Figure 16. The reasons for the apparent deviation between the S7 and the rest of the experiments is not clear. As we can see in Figure 15, the S7 seems to have a weaker dependency on the subcooling than the rest. This could be due to the high steam mass fluxes of the S7 compared to the rest of the sub-sonic experiments.

A fitting performed using dimensional variables led to negligible dependencies with the diameter and steam mass flux. Thus, the f_c/f_{bl} ratio was fitted only as a function of the subcooling, leading to equation (20), which has a similar MPE of 7.3 % as equation (19).

$$\frac{f_c}{f_{bl}} = \frac{4.27}{\Delta T^{0.30}} \quad (20)$$

Figure 15: Ratio between the collapsing f_c and bubble-life f_{bl} frequency as a function of the subcooling ΔT . Mean values and error bars are obtained by fitting the mean, maximum, and minimum radius from Figure 14.

Figure 16: Comparison between the f_c/f_{bl} ratio measured in SEF to the one predicted by equation (20).

The maximum bubble radius R^* was measured by Fukuda [12], who proposed a correlation given by $(R^*/d^{1.05}) \propto (G^{0.05}/\Delta T)$. A preliminary fitting of the R^* values measured in SEF led to a similar exponent for d of about 1.03, which was later fixed to 1 to provide a non-dimensional R^*/d factor. Fitting of this factor using the non-dimensional Ja , Re and We number led to equation (21), which shows very good agreement with the SEF and Fukuda data, Figure 17.

$$\frac{R^*}{d} = 0.18 \cdot Ja^{-0.70} Re^{-0.17} We^{0.22} \quad (21)$$

Measurements of R^* done by Simpson et al. [13] show that the maximum bubble radius *decreases* as the pool temperature increases (Table 1 and 2 in [13]). On the other hand, bubble profiles presented at the same paper show that the maximum bubble radius *increases* with the pool temperature (Fig. 3bc in [13]), a behavior with is in agreement with the current SEF experiments (Figure 14) and with Fukuda's data. A possible cause for the apparent contradiction in Simpson et al. data is that the temperature given in Table 1 in [13] is the subcooling, not the pool liquid temperature. This could also explain the large scatter observed in the Simpson et al. frequency data (Figure 13). Due to the current uncertainty, Simpson et al. data on R^* was no included in this work.

A fitting performed using dimensional variables led to equation (22), which resulted in a similar MPE of 6.2 % as equation (21). In both of these equations, the resulting exponent of the steam mass flux G is larger than in the previous correlation proposed by Fukuda.

$$\frac{R^*}{d} = 3.5 \frac{G^{0.27}}{\Delta T^{0.68}} \quad (22)$$

Figure 17: Comparison between the maximum bubble radius R^* measured by Fukuda [12] and SEF against the ones predicted by equation (21). MPE is only computed for the SEF data.

4.3. Pressure gradient

The pressure gradient across the bubble can be computed as $\nabla P = \rho_L \bar{g}$ when a bubble is placed in a stagnant liquid with a given hydro-static pressure field. Wall effects can be added by taking into account the distance between the bubble center and wall [20, 22]. Computing ∇P for a bubble located in a moving fluid (e.g. flow through a hydrofoil) is a complex task. The approach taken by most authors is to split the pressure in two terms: one considering the fluid motion without a bubble, the other with the local changes caused by the bubble [25]. Flows with strong pressure gradients allow neglecting the second term, leading to $\nabla P \approx -\rho_L \bar{U}_L \nabla \cdot \bar{U}_L$, which can be obtained with measurements or numerical simulations [24].

The direction of collapse is determined by the direction of the pressure gradient. Since the video images show that the collapse is in the direction of the injection x , we will assume $(\nabla P_x, 0, 0)$. Analysis of the PANDA experiments showed that the liquid velocity induced by a steam injection can be modelled with standard jet equations [10]. Since the pressure gradient is negligible in jet flows, the assumption of $\nabla P \approx -\rho_L \bar{U}_L \nabla \cdot \bar{U}_L$ would not be an appropriate one for this case. For example, Tollmien's solution of a jet is based on neglecting the pressure term but keeping the

turbulent stresses. Therefore, ∇P should be computed based on the time-dependent forces caused by the bubble motion.

A force balance over the oscillatory bubble contains too many uncertainties to give an accurate estimate of ∇P : the drag coefficient, liquid velocity, wall effects while the bubble remains attached to the injection hole, etc. Assumptions can be made for each parameter, but the validity of each cannot be assessed independently since only the prediction with combined factors can be compared to the experiment.

Due to the uncertainties associated with the force balance, the pressure gradient was derived directly from the measured EMS. Based on equation (18), the EMS given by equation (11) can be written as

$$M_{eff} = n f_c \int_0^{t_{bl}} \nabla P \left(\frac{4}{3} \pi (R^* \sin(\pi f_{bl} t))^3 \right) dt \quad (23)$$

We assume that ∇P remains constant during the bubble collapse with a value of $\langle \nabla P \rangle$. With such assumption, integration of equation (23) leads to

$$M_{eff} = \frac{8}{9} \frac{f_c}{f_{bl}} n \langle \nabla P \rangle R^{*3} \quad (24)$$

where $\langle \nabla P \rangle$ can be computed as a function of the measured M_{eff} and equations (20) and (22) for f_c/f_{bl} and R^* respectively. A correlation of the resulting $\langle \nabla P \rangle$ as a function of the dimensional injection parameters resulted in equation (25), which is compared to the SEF data in Figure 18.

$$\langle \nabla P \rangle = 0.028 \frac{G^{1.20} \Delta T^{1.93}}{d^{1.08}} \quad (25)$$

Figure 18: Comparison between the average pressure gradient $\langle \nabla P \rangle$ estimated from the SEF data to the one predicted by equation (25).

Using equations (20), (22), (24) and (25), the condensation regime coefficient based on Kelvin Impulse theory C_{KI} becomes

$$C_{KI} = 3.48 \frac{G^{0.01}}{d^{0.08} \Delta T^{0.41}} \quad (26)$$

which is similar to equation (8), based on direct fitting of the C experimental data. The EMS and C coefficient obtained with Kelvin Impulse theory are presented in Figure 19. We cannot claim a prediction, since the $\langle \nabla P \rangle$ used in the equations was calibrated to fit the M_{eff} data. However, Figure 19 supports the individual correlations proposed for f_c/f_{bl} , R^* , and $\langle \nabla P \rangle$, since their combined solution leads to an adequate trend of the EMS.

Figure 19: Comparison between the sub-sonic EMS M_{eff} and the condensation regime coefficient C measured in SEF to the ones one predicted by equations (24) and (26).

4.4. Bubble velocity

The bubble velocity and heat transfer coefficient (section 4.5) were not needed for the prediction of the EMS. Nevertheless, they were included in this paper for completeness of the SEF data post-processing and to enable further analysis by other authors.

The location of the bubble center as a function of time is presented in Figure 20. Low subcoolings and high steam mass fluxes allowed the bubble to travel further. It should be noted that the last data points of each transient do not correspond to the end of the collapse phase, since this one could not be captured in the image processing as discussed in Section 4.2 and Figure 14.

Figure 20: Location of the bubble center. Mean values and error bars are calculated based on the analysis of 9 independent bubble transients. Red markers correspond to the detachment time predicted by equation (16).

The instantaneous bubble velocity was derived from Figure 20 and is presented in Figure 21. Since the Δx at each step is similar to the error of x , the velocity was computed by using every 4th data point, which reduces the error bars at the expense of lower time resolution.

At the beginning of the cycle the bubble is attached to the injection hole. Thus, its translation velocity is mainly attributed to its expansion. The fastest acceleration was found at the beginning of the detachment. Once completely detached, the bubble moves at a quasi-constant velocity. This observation agrees well with the work from Yuan et al. [16], who assumed that the bubble acceleration at the detaching phase is zero (allowing them to neglect the inertia term in the bubble force balance). However, the assumption done Yuan et al. [16] that the bubble velocity is constant during all its cycle is not confirmed in Figure 21.

Figure 21: Instantaneous bubble velocities. Error bars are obtained through propagation of the ones from Figure 20. Red markers correspond to the detachment time predicted by equation (16).

The average velocity $\langle U_b \rangle$ was computed using all the data points from each bubble cycle. Normalization of $\langle U_b \rangle$ by the measured maximum bubble radius R^* and collapsing frequency f_c was observed to produce a constant value for a given steam mass flux G . Thus, the normalized velocity was fitted as a function of the dimensionless Ja , Re , and We numbers, leading to equation (27), which is compared to the experimental data in Figure 22.

$$\frac{\langle U_b \rangle}{R^* f_c} = 148 \cdot Ja^{0.10} Re^{-0.84} We^{0.71} \quad (27)$$

The low exponent of the Ja number in equation (27) suggests a small dependency on the sub-cooling ΔT . The exponents of Re and We also suggest a weak effect of the diameter d . Therefore, fitting of the velocity using dimensional quantities was only done as a function of G , leading to

$$\frac{\langle U_b \rangle}{R^* f_c} = 0.16 \cdot G^{0.53} \quad (28)$$

which showed a MPE of 10.5 %.

Figure 22: Comparison between the average bubble velocity $\langle U_b \rangle$ measured in SEF to the one predicted by equation (27).

4.5. Heat transfer coefficient

The time-dependent heat transfer coefficient h was computed using the mass balance given by equation (29),

$$h = \frac{\rho_s(Q_b \Delta t - \Delta V_e) h_{fg}}{A_e \Delta T \Delta t} \quad (29)$$

where sub-index e corresponds to the ellipsoid's parameters and Q_b to the volumetric steam flow rate injected into the bubble. Before detachment Q_b was assumed to be equal to the steam flow at the injection holes Q_i . After detachment then neck connecting the injection hole and bubble was observed to vary with the steam mass flux and subcooling, leading to an uncertain range of $0 < Q_b < Q_i$. Determination of Q_b for all regimes would require a separate image processing study for the neck, something which is beyond the scope of this work. Instead, we present the results for the two limiting conditions, $Q_b = 0$ and $Q_b = Q_i$, which should serve as a starting point for further studies.

The results given by equation (29) show that h varies by 3-10 times during the analyzed transient (Figure 23). Before detachment, the dependency with the subcooling was reduced as the ΔT increased, especially in the $75 \text{ kg/(m}^2\text{s)}$ case.

After detachment, the assumption of $Q_s = Q_i$ led to a smooth transition of h , suggesting that $Q_s = Q_i$ might be applicable in some cases. Nevertheless, the cases with $\Delta T = 14$ °C are best represented with the $Q_s = 0$ assumption since in these cases the bubbles were observed to clearly break the neck and separate from the injection holes, Figure 6.

Figure 23: Heat transfer coefficient obtained using equation (29) and assuming that the conditions after detachment are (a) $Q_b = Q_i$ and (b) $Q_b = 0$. Error bars are obtained through propagation of the ones from Figure 14.

Estimations of the heat transfer coefficient done by Fukuda [12] and Simpson et al. [13] were done for its time-averaged value $\langle h \rangle$. In both cases, the steam flow into the bubble Q_b was assumed to be constant, equal to the one at the injection hole. In the case of Fukuda, the bubble surface area was taken at its maximum value R^* , equation (30).

$$\langle h \rangle = \frac{A_i G h_{fg}}{(4\pi R^{*2}) \Delta T} \quad (30)$$

The use of R^* suggests an under-estimation of $\langle h \rangle$, since bubble sizes during the rest of the transient are always smaller. A non-dimensional form of $\langle h \rangle$ was also proposed by Fukuda by inserting the $(R^*/d^{1.05}) \propto (G^{0.05}/\Delta T)$

correlation proposed in his work in equation (30), adding liquid properties, and calibrating a constant to 43.78 for the case of $T_L = 30^\circ\text{C}$.

$$\frac{\langle h \rangle d}{\lambda_L} = 43.78 \left(\frac{dG}{\rho_L \nu_L} \right)^{0.9} \frac{c_{pL} \Delta T}{h_{fg}} \quad (31)$$

The benefit of equation (31) is not clear since it will lead to deviations at liquid temperatures different than 30°C . In fact, the $\langle h \rangle$ data presented by Fukuda (Fig. 5 in [12]) is based on equation (30), whereas using equation (31) leads to significant deviations. Therefore, we recommend not using equation (31). This leaves us with no directly usable $\langle h \rangle$ correlation from Fukuda since the constant of proportionality for the R^* correlation needed for equation (30) is not given in his paper.

Simpson et al. [13] computed the bubble area as an “integral average of the interfacial area over a complete cycle”, which we will refer to by $\langle A_e \rangle$. They did not provide an explicit correlation for $\langle h \rangle$, but their data and fitting curve can be well approximated with equation (32).

$$\langle h \rangle = \frac{A_i G h_{fg}}{\langle A_e \rangle \Delta T} \approx 89 \Delta T^2 + 3400 \Delta T \quad (32)$$

In this work, the time-averaged heat transfer coefficient $\langle h \rangle$ was computed using equation (33).

$$\langle h \rangle = \frac{1}{N} \sum_{t/t_d > 0.36}^{t/t_d < 1} h \quad (33)$$

Due to the uncertainty on Q_b , $\langle h \rangle$ was computed only for the detachment phase. Since the experiments performed using 700 fps video could not capture the large initial values of h shown in Figure 23, obtained at 2800 fps, the averaging was done from $t/t_d > 0.36$, where data is available for all SEF experiments. Fitting of the Nusselt based $\langle h \rangle$ as a function of the non-dimensional Ja , Re and We numbers resulted in equation (34), which is compared to experimental data in Figure 24.

$$\frac{\langle h \rangle d}{\lambda_L} = 5.5 \cdot Ja^{0.41} Re^{0.80} We^{-0.11} \quad (34)$$

Direct comparison between SEF and Simpson et al. and Fukuda data should be done with caution due to their assumption of constant steam flow into the bubbles and to the fact that the $\langle h \rangle$ values obtained in this work are limited to the detachment phase. Figure 24 might only indicate a good agreement in the order of magnitude of $\langle h \rangle$. Further analysis and experimental data is needed to provide a more general correlation for $\langle h \rangle$.

Fitting of the Nusselt based $\langle h \rangle$ as a function of the injection parameters leads to equation , which showed an MPE of 9.7 %.

$$\frac{\langle h \rangle d}{\lambda_L} = 29000 \cdot G^{0.47} \Delta T^{0.39} d^{0.26} \quad (35)$$

Figure 24: Time-averaged heat transfer coefficient $\langle h \rangle$ obtained by Fukuda [12], Simpson et al. [13] and in SEF compared with the correlation adapted from Simpson et al. data, equation (32) and the correlation proposed in this work, equation (34). MPE is only computed for the SEF data.

5. CONCLUSIONS

Prediction of the Effective Momentum Source (EMS) induced by steam condensation in the oscillatory bubble regime is necessary for the modelling of the pressure suppression pool behavior. This is especially relevant for Boiling Water Reactors (BWR), where the development of thermal stratification or mixing during a steam injection through spargers can affect the performance of the suppression pool. To measure the EMS, a Separate Effect Facility (SEF) was built at LUT, Finland. Video imaging and pressure transducers were also used to measure other bubble dynamics parameters.

The EMS was correlated to the steam injection parameters through the non-dimensional C coefficient, which represents the ratio of the EMS to a theoretical steam momentum at the injection holes. This coefficient showed that sub-sonic regimes have a stronger dependency on the Jakob number (i.e. subcooling) than sonic ones. A correlation of C as a function of the Jakob number was proposed for sub-sonic regimes, which showed good agreement with the experimental data. Sonic regimes presented a quasi-constant C coefficient of about 0.84. However, this conclusion is limited to a steam mass flux of about 320 kg/(m²s). Experiments at larger Jakob numbers and steam mass fluxes are needed to develop a more general correlation.

Another correlation for sub-sonic C coefficients was developed based on the Kelvin Impulse theory. This was observed to be dependent on the frequency ratio (bubble collapse to bubble-life frequency), maximum bubble radius, and pressure gradient across the bubble. Correlations for each parameter were provided and compared to other available data from the literature. Since the pressure gradient was not measured directly in the experiments, it was derived using the measured EMS. Additional correlations for the bubble velocity and heat transfer coefficient (not necessary for the EMS prediction), were also provided. In the heat transfer coefficient, further experiments are

required to reduce the uncertainties associated with its strong time-dependency and the steam flow entering the bubble.

ACKNOWLEDGEMENTS

This work was performed with financial support from the Swedish Radiation Safety Authority (Strålsäkerhetsmyndigheten, SSM) under the NORTHET RM3 project and the Nordic Nuclear Safety Research (NKS) under the COPSAR project.

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