Heat Transfer Measurement in a Three-Phase Direct-Contact Condenser under Flooding Conditions

Hameed B. Mahood^{1,2*}, A. N. Campbell¹, A.O. Sharif^{1,3} and R. B.Thorpe¹

¹Centre for Osmosis Research and Applications (CORA), Chemical and Process Engineering Department, Faculty of Engineering and Physical Sciences, University of Surrey, Guildford GU2 7XH, UK

[*hbmahood@yahoo.com](mailto:hbmahood@yahoo.com)

²University of Misan, Misan–Iraq

³The Qatar Foundation, Qatar Energy and Environment Research Institute, Qatar

Abstract

The transient temperature distribution and volumetric heat transfer coefficient during the inception of flooding in a three-phase bubble type direct contact condenser have been experimentally investigated. The flooding mechanism and the factors affecting the onset of flooding of the three-phase direct contact column are not considered. A short Perspex column of 70 cm total height and 4 cm internal diameter utilising two immiscible fluids was studied. Pentane vapour with initial temperatures of 40°C, 43.5°C and 47.5°C was the dispersed phase and tap water at a constant temperature (19°C) was the continuous phase. Only 48 cm of the column was used as the active height and different mass flow rates of both phases were used. The experimental results showed that the instantaneous temperature distribution along the direct contact column tends to be uniform when the direct contact column is working under flooding conditions. Furthermore, the volumetric heat transfer coefficient increases as the dispersed mass flow rate is increased towards the flooding limit and remains constant along the column height. In addition, the dispersed phase mass flow rate that leads to flooding increased with increasing mass flow rate of the continuous phase. The initial temperature of the dispersed phase did not have a considerable effect on the flooding inception limit under the present experimental conditions.

Keywords: Flooding; three-phase direct contact heat exchanger; transient temperature distribution; volumetric heat transfer coefficient

1. Introduction

Conversely, to the indirect contact heat exchange, direct contact heat exchange relates to the transportation of thermal energy between two or more fluid streams when they are brought into intimate contact with one another. Accordingly, heat exchangers can be split into two main types: classical, or surface type, and direct contact type. In the former, the two fluid streams, (hot and cold) are completely separated by a metallic or solid barrier, and heat is transformed through it. Therefore, the ability of this type of exchanger to extract the thermodynamic potential energy is constrained due the heat transfer resistance of the metallic surface, which is also exposed to fouling, corrosion and thermal stresses, especially when it used over a high temperature range.

Practically, the direct contact heat exchanger (condenser in the present case) can be split into two main types: single component direct contact condenser, when the same chimerical species is used, for example condensing of steam in water. The second type is two-components (or three-phase) direct contact condenser. In this type, two completely immiscible fluids are used, for example condensing of pentane vapour bubbles in subcool water.

The condensation of a vapour bubble in another immiscible liquid (i.e. a two-component system) is different from when it condenses in its own liquid (i.e. one-component system). This is because in a two-component system a confined vapour/liquid two-phase bubble is formed. Hence, the liquid condensate remains within the mother bubble, in contrast to the one-component system where the condensate mixes with the surrounding fluid. The vapour and liquid within the two-phase bubble are completely separated due to the effect of gravity. As shown in Fig. (1), vapour collects at the top of the two-phase bubble whilst liquid spreads and accumulates at the bottom with a monolayer film of condensed liquid covering the whole bubble surface [1, 2]. Therefore, three distinct phases (pentane liquid-pentane vapour-water) are present in the process and a three-phase direct contact condenser is emerged similar the case of three-phase direct contact evaporator [3-7].

The three-phase, direct contact condenser, however, can be classified into to three main types [8]: drops, film and bubble type direct contact condensers. Among these, the bubble type three-phase condenser has a relatively high efficiency, is simple in design and construction and low in cost [9].

Fig. 1. Configuration of two-phase bubble condensation in an immiscible liquid (a represents the two-phase bubble radius, U denotes the continuous phase velocity, V_r and V_θ are the radial and tangential velocity components, θ is the angle in spherical coordinate).

Generally, the three-phase bubble type direct contact condenser (Fig. 2) consists of a vertical cylindrical column and injection devices such as nozzles or plate orifices fixed at the top and bottom of the column for injection of the dispersed and the continuous phase (Fig. 2). Vapour bubbles, often a light hydrocarbon vapour, is commonly used as a dispersed phase while another immiscible liquid (usually water) as a continuous phase. The temperature of the continuous phase is maintained less the saturation temperature of the dispersed phase.

Driven by the buoyancy force, the dispersed phase (as two-phase, vapour/liquid bubbles, Fig (1)) travels up the column through the falling continuous phase and heat is absorbed by the continuous phase, which results in a portion of vapour bubbles instantaneous condensing along the column. Thus, the vapour bubbles are condensed completely in an active height, which depends on several parameters, such as temperature difference, mass flow rate ratio and the physical properties of both phases. The condensate is collected and returned to the closed cycle, while the continuous phase is drained out from the column bottom.

Fig. 2. Schematic diagram of the bubble type three-phase direct contact condenser.

The continuing enhancement of energy recovery cycles that exploit low grade energy resources requires an efficient heat absorption and rejection system. Implementation of a conventional surface type heat exchanger, evaporator or condenser has many disadvantages. The low efficiency, fouling, corrosion problems, high cost and high heat transfer resistance are the most important shortcomings that emerge as a result of the metallic barriers involved. On the other hand, a direct contact heat transfer device offers a high heat transfer area and reduces or eliminates fouling and corrosion problems. They can also work with very low temperature differences and subsequently can enhance the cycle efficiency. Direct contact heat exchangers clearly have many advantages over surface type heat exchangers and can also be efficiently used in places where the surface heat exchanger cannot be. Nevertheless, the main obstacles still facing the direct contact heat exchanger are: (i) the two-phases must be completely immiscible, and (ii) there is a lack of sufficient experimental data or theoretical design formulae available. However, in spite of the considerable literature available concerning the three-phase direct contact evaporator, there has been almost no attention been

paid to the three-phase direct contact condenser. Although extensive investigations have focused on the phenomenon of a single two-phase bubble [10-18] and bubble swarm [19-22] condensing in another immiscible liquid, which represents the base that the three-phase direct contact condenser can build on. Only the work of Sideman and Moalem [23] has been found to be directly relevant to a three-phase direct contact condenser. They developed a mathematical model to describe the direct contact condensation of a multi-train two-phase bubble in another immiscible liquid. Although this study is not representative of a real three-phase direct contact condenser, it has improved the general understanding of the condensation of a swarm of two phase bubbles in an immiscible liquid.

Recently, Mahood and co-authors [9, 24-29] investigated theoretically and experimentally [24-28] the various characteristics of thermal performance of the three-phase direct contact condenser. The thermal efficiency and the capital cost of the three-phase direct contact condenser have been recently researched [9]. Relatively high condenser efficiency (about 75%) with a relatively low mass flow rate ratio (about 25%) has achieved. The capital cost was found to be decreased with increasing of the dispersed phase mass flow rate. The steady-state and the transient temperatures distribution along the three-phase direct contact condenser have studied experimentally and theoretically [24, 25], respectively. The results showed for both cases that the mass flow rate ratio and the holdup fraction have a significant effect on the condenser outlet temperature. Also, the volumetric heat transfer coefficient in the condenser has studied experimentally for both steady-state and transient operation modes [26, 27], respectively. The results indicated that the volumetric heat transfer coefficient decrease upon moving up the condenser and with time with a positive effect of mass flow rate ratio.

As mentioned above, author et al. have extensively researched the heat transfer characteristics of the three-phase direct contact condenser under different operational conditions. However, the heat transfer during flooding and the flow limitation of the condenser have excluded. In this paper, the heat transfer during flooding in the three-phase direct contact condenser is studied experimentally. The flow limitation of both continuous and dispersed phases was determined. No attention is directed towards the flooding phenomena themselves. The transient temperature distribution, phase maps identifying flooding conditions and the volumetric heat transfer coefficient associated with flooding are estimated.

1.1 Flooding Phenomena

It is economical and practical for the bubble type direct contact condenser to be operated at a maximum possible holdup ratio to increase the condenser performance. It is widely reported that the column performance is well described by a volumetric heat transfer coefficient [6]. Practically, the volumetric heat transfer coefficient increases directly with increasing the holdup ratio in the column [6, 29-31]. On the other hand, when the direct contact column operates with a high holdup ratio, the probability of the undesirable flooding phenomenon is increased. This hampers the direct contact column's performance.

Two mechanisms could lead to the inception of flooding in the three direct contact columns, depending on the critical velocity of each individual phase. The critical velocity can be defined as a maximum velocity that can be achieved by a given direct contact system and it is a function of the bubble size, the flow rate and the physical properties of the phases. The first mechanism depends on the reduction in the dispersed phase (bubbles) upward velocity due to the interactions within a swarm of two-phase bubbles. The two-phase bubbles move closer together when the dispersed phase flow rate increases, which results in a further dispersed phase slowing. Bubbles are swept down and drain out with the continuous phase from the bottom of the column. Therefore, the danger from this form of flooding form is the increased loss of the working fluid. The second scenario assumes that the continuous phase is swept upwards because of a high dispersed phase flow rate. This occurs when the dispersed phase velocity passes the critical velocity. The result of this flooding type lies in a change of geometry and makes the heat transfer relationships available invalid. Accordingly, flooding can be defined as the case when the continuous phase is completely held up by the dispersed phase or the dispersed phase is swept backward by the continuous phase.

In practice, flooding can be diagnosed by monitoring the pressure difference across the column or the temperature of the column. Depending on the flooding type, the temperature at the bottom or top of the column may increase due to the blocking of vapour flow by a high continuous phase flow rate in first type, or as a result of holding up the continuous phase by a high dispersed phase flow rate in the second.

It is necessary to avoid both flooding forms throughout the direct contact spray column design. The first flooding type can be avoided for a given condenser cross-sectional area by increasing the column height or by using more than one continuous phase inlet and outlet. The second type of flooding is avoided by ensuring that the space between neighbouring

bubbles is large enough to prevent them from touching each other. The suitable spacing was found to be at least twice the initial radius of a bubble [32].

Flooding is a very complex phenomenon and depends strongly on the physical properties of the two phases, the column geometry, the configuration of the inlets and outlets of the two phases, the column diameter and the two-phases' velocities. Therefore there is a significant divergence in this case from the results for a system without phase change. The simple empirical expressions are only useful as a guide and accurate chemical process equipment design depends on relevant experimental data.

Although, flooding phenomenon has been widely investigated over a number of decades especially for isothermal (gas-liquid) system a considerable uncertainty regarding the mechanism responsible on the flooding inception still existing [33]. However, depending on tube geometry and fluids properties, three different mechanisms for flooding inception were suggested; droplet dynamics, film flow dynamics and wave dynamics [34]. Nevertheless, there is no unique correlation can be used to predict the flooding inception. For a wide variety of fluid pairs and for a vertical tube with small diameter, the correlation of Hewitt and Wallis [35] have widely used.

For the case of flooding of the three-phase direct contact heat exchanger and with absence of especial correlations, Wright [32] tested the three most cited approaches which have been developed for a system without phase change by [36-38]. A great conflict in the results of these correlations was found. A more relevant study is reported by Goodwin et al. [31], where the flooding limit (downward flooding) of a large diameter spray column direct contact evaporator was evaluated experimentally. The variation of the point of inception for flooding with the two-phase mass flow rate and the column pressure for the downward flooding type was investigated. They observed that the possibility of flooding decreases with increasing the dispersed phase mass flow rate and the column pressure for a constant mass flow rate ratio.

2. Experimental Facility

2.1 Experimental Setup

Figure 3a shows the experimental layout. The detail of experimental setup and procedure can be found in the previous research [24, 25]. Briefly, the experimental rig consists of two main loops. The first loop is for the dispersed phase, and it uses liquid pentane as a working fluid. There is a pentane storage tank (10 L), a liquid pentane circulation pump, a control valve, non return valve, a heating or vaporisation vessel, trace heater and connection tubes. The second

loop is for the continuous phase; it consists of a large water storage tank, pump, flow meters and connection pipes. In addition, a Perspex cylindrical column of 48 cm active height from 70 cm total height is used as a direct contact condenser (test section). Eight K- type, calibrated thermocouples, connected to a PC via an eight channel data logger, were used to measure the temperature along the test section, the vapour in the heating vessel and the vapour just before injection in to the column.

The heating vessel is a pool boiler type (100cm x 50 cm) and contains a long (6 m length and 6 mm diameter) copper coil. The heating vessel is completely full with water, which is used as a media for heating up and vaporising liquid pentane. It is heated by two electric heaters (3 kW each). The heating power is controlled by a thermostat through a control panel. One end of the copper coil is connected to the liquid pentane storage tank via the liquid pentane circulation pump and the flow rate is controlled by a ball valve. The non return valve is installed at the inlet of the heating vessel to prevent dispersed phase back flow caused by the heating vessel pressure head, especially during the shutdown period (pentane in liquid state) when the injection valve is closed. The second end of the coil is connected to the test column (direct contact condenser) by a short externally heated tube. The vapour (dispersed phase) injection temperature and pressure were measured using a thermocouple and pressure gauge respectively. The short injection tube is heated by a variable temperature trace heater.

2.2 Experimental Procedure

The experiment began by preparing the continuous phase (water) in its large size storage tank (160 L volume) and maintaining its temperature at a desirable value (19 °C). The large storage tank made the water temperature approximately invariant during each run (about 3 minutes). The cooling water (continuous phase) was re circulated through the test section to maintain its temperature. The water flow rate and the water level (48 cm) in the direct contact column (test section) were determined and controlled by adjusting the two flow meters (one for water inlet and second for water outlet)

The heating vessel was gradually warmed up by adjusting the power of electric heaters, until the temperature reached the desired level. The liquid pentane (dispersed phase) was then pumped to the heating vessel, where it warmed and evaporated (pentane boils at 36.5 °C) and was then injected via the injection tube in to the test section (direct contact condenser). Its temperature and pressure were measured before injection. Because of the trace heater, the injection temperature of the dispersed phase was maintained constant during each run. The

desired injection dispersed flow rate was specified and controlled by a calibrated ball valve in a similar technique to that used by [24, 25, 39-41].

At the moment of vapour injection in to the test section, the temperature along the test section is measured using five calibrated K-type thermocouples, fixed at different positions from the bottom of the column (see Fig. 3b): TC1@0 cm (for continuous phase outlet), TC2@12 cm, TC3@24 cm, TC4@36 cm and TC5@48 cm (for continuous phase inlet). Three more thermocouples were used: one for measuring the condensate temperature at a height of 52 cm and the other two are for the injected vapour temperature and for the dispersed phase temperature in the heating vessel. All these thermocouples were connected to an eight channel data logger to display the measured temperature directly on a PC.

The condensate was collected, weighted and divided by the duration of the experiment to determine the dispersed phase mass flow rate. A conical separator was used to recover nearly all the pentane from the drained water. The pentane collected as a condensate during each run was directly sent back to the liquid pentane storage tank to use in a continuous run or to be stored and used in another run.

The uncertainty of the thermocouples and the continuous phase mass flow rate measurement are given in tables 1 and 2. For the dispersed phase mass flow rate measurement, the mass balance after each run was used. The total error in the measurements, which consists of the error in the digital scale, the loss of condensate due to the miscibility of pentane in water (which is very small), the time of the run and the possibility of draining the condensate with the continuous phase has also been calculated. The inaccuracy of the dispersed phase mass flow rate is estimated to be $\pm 11\%$.

Fig. 3a . Schematic diagram of the experimental rig.

Fig. 3b. Thermocouple locations along the direct contact condenser column.

Table 1 Inaccuracy of the thermocouples

$T(^{\circ}\text{C})$	Inaccuracy %
T_{c1}	± 0.440
T_{c2}	± 0.508
T_{c3}	± 0.325
T_{c4}	± 0.359
T_{cond}	± 0.582
T_{di}	± 0.480
T_{ci}	± 0.353

Table 2 Inaccuracy of the continuous phase rotameter

$\dot{m}_c$ (kg/min)	Rotameter inaccuracy %
0.0564	± 6.000
0.107	± 10.608
0.201	± 8.636
0.286	± 4.666
0.381	± 4.750

3. Results and Discussion

3.1 Transient temperature distribution

The present investigation is concerned with the heat transfer characteristics of a three-phase direct contact condenser under flooding conditions. The mechanism and the factors affecting the inception of flooding of the three-phase direct contact condenser is outside the scope of the present study.

The experimental time dependent temperature distributions after and during the flooding conditions are shown in Figures (4-6). It is clear that the temperature distribution along the direct contact condenser during the flooding becomes more uniform as a result of an almost completely mixed column. The cooling water is halted by the upward force produced by a high vapour (dispersed phase) mass flow rate. Therefore, a static water column above the dispersed phase injection is formed. Consequently, the cooling rate which is achieved by the flowing continuous phase is reduced.

Visually, the inception of flooding is preceded by a transition from large spherical to spherical cap bubbles which then developed to a long slug. The small column diameter, of course, helps to create the long slug when the flooding limit is reached. As shown in the figures (Figs. (4-6)), the inception of flooding strongly depends on the mass flow rate of both phases. Clearly, at a constant continuous phase mass flow rate, a higher dispersed phase mass

flow rate results in an earlier inception of flooding (see sub-figures a & b in Figs. (4-6)). This can be attributed to the larger upward force produced by the higher momentum of vapour. This causes the continuous phase to be suspended and then pushed up. In addition, the high dispersed phase flow produces large bubbles which help to accelerate the inception of flooding. Visual observation indicated that a vapour pocket emerged just above the sparger as a result of coalescence of the large non-uniform bubbles that were produced by a high dispersed phase mass flow rate. This structure broke down to large bubbles or sometimes slugs depending on the force produced by the continuous phase. However, coming back to Figs. (4-6), it is obvious that, for example, at $\dot{m}_c = 0.0564 \text{ kg/min}$ and $T_{di} = 40^\circ\text{C}$, the inception of flooding occurred after about 150s and 90s, from the beginning of runs with a continuous mass flow rate of 0.04035 kg/min and 0.04872 kg/min respectively. This is completely consistent with results for other initial temperatures and continuous mass flow rates. For example, the inception of flooding was observed after 170 s and 150s, for a continuous mass flow rate 0.039 kg/min and 0.0472 kg/min respectively ($T_{di} = 43.5^\circ\text{C}$). Finally, for $T_{di} = 47.5^\circ\text{C}$, and continuous mass flow rates 0.0408 and 0.054 kg/min the inception of flooding was seen after 155s and 110s. The same behaviour was found for another continuous phase mass flow rate ($\dot{m}_c = 0.107 \text{ kg/min}$).

The temperature of the continuous phase along the column increased with time and a more rapid rise was seen over the first 50s, as observed by Mahood et al. [25]. This behaviour was also noticed during the experiments with flooding. After the onset of flooding, the thermocouples registered nearly the same temperature (see Fig. (4-6), after flooding inception point), with a notable decrease in the continuous phase outlet temperature (T_{c1}) also observed. This is because at flooding, the continuous phase liquid is stopped to reaching the bottom of the column, which leads to heat up the semi static water (continuous phase) column. Therefore, the injected dispersed phase (vapour) would not undergo a convective condensation and the dispersed phase would be almost vapour when is reached the top of the column, and a further condensation would take place by a relatively cooled continuous phase. This simply means, that at flooding the direct contact condensation will take place at the top of the column rather than at the bottom. Nevertheless, the temperature of the continuous phase seemed to be uniform for the case of complete flooding (after a while from inception of flooding), where the temperature of the continuous phase column had nearly the same temperature (see Figs. (4-6), after the inception of flooding).

No considerable effect of initial dispersed phase temperature on the inception of flooding is shown by the results (Figs. (4-6)). This consists with that the direct contact condensation is a latent heat dominant [24-26, 42].

Schematically Figs. (7-9) show the maps of the mass flow rates of two phases (which result in flooding) for three different dispersed initial temperatures. Again a high continuous mass flow rate is clearly requires a high dispersed phase flow for a flooding to occur.

Figure 10 shows the relationship between the dispersed phase mass flow rate and the continuous phase mass flow rate for a different initial dispersed phase temperatures, at the onset of flooding. Except at a high continuous mass flow rate (nearly 0.38116 kg/min), the initial temperature of the dispersed phase seems to have very little effect on the occurrence of flooding within the temperature range of the present experiments.

As shown in Figures (4-6), the temperature distribution during flooding tends to be more uniform with time due to the mixing along the column. This, of course, will change the heat transfer rate in the column and subsequently change the column performance indicator, i.e. volumetric heat transfer coefficient. The volumetric heat transfer coefficient, however, increases with holdup towards the flooding limit [31].

Fig. 4. Transient temperature distribution at, and during, DCC flooding for ($T_{di} = 40^{\circ}\text{C}$) and
 (a): $\dot{m}_c = 0.0564 \text{ kg/min}$ & $\dot{m}_d = 0.04035 \text{ kg/min}$, (b): $\dot{m}_c = 0.0564 \text{ kg/min}$ & $\dot{m}_d =$
 0.04872 kg/min and (c): $\dot{m}_c = 0.107 \text{ kg/min}$ & $\dot{m}_d = 0.0571 \text{ kg/min}$

Fig. 5. Transient temperature distribution at, and during, DCC flooding for ($T_{di} = 40^{\circ}\text{C}$) and
 (a): $\dot{m}_c = 0.0564 \text{ kg/min}$ & $\dot{m}_d = 0.039 \text{ kg/min}$, (b): $\dot{m}_c = 0.0564 \text{ kg/min}$ & $\dot{m}_d =$
 0.0472 kg/min and (c): $\dot{m}_c = 0.107 \text{ kg/min}$ & $\dot{m}_d = 0.0573 \text{ kg/min}$

Fig. 6. Transient temperature distribution at, and during, DCC flooding for ($T_{di} = 40^{\circ}\text{C}$) and
(a): $\dot{m}_c = 0.0564 \text{ kg/min}$ & $\dot{m}_d = 0.0408 \text{ kg/min}$, **(b):** $\dot{m}_c = 0.0564 \text{ kg/min}$ & $\dot{m}_d =$
0.054 kg/min and **(c):** $\dot{m}_c = 0.107 \text{ kg/min}$ & $\dot{m}_d = 0.046 \text{ kg/min}$

Fig. 7. Map of mass flow rates investigated showing flooding and non flooding cases ($T_{di} = 40^\circ\text{C}$)

Fig. 8. Map of mass flow rates investigated showing flooding and non flooding cases ($T_{di} = 43.5^\circ\text{C}$)

Fig. 9. Map of mass flow rates investigated showing flooding and non flooding cases ($T_d = 47.5^\circ\text{C}$)

Fig. 10. Effect of the initial temperature of the dispersed phase on flooding inception

3.2 Volumetric heat transfer coefficient prediction

To calculate the volumetric heat transfer coefficient of the three-phase direct contact condenser, the column height was divided into four equal sub-volumes. An energy balance over each individual sub-volume was performed based on the following assumptions:

- The sensible heat is small in comparison with the latent heat of the condensing vapour; therefore, it is negligible in the calculations. The saturation temperature (T_{di}) of each column sub-volume is calculated depending on the local pressure by using the Antoine Equation.
- Both the continuous phase and the dispersed phase mass flow rates are assumed constant along the column. This is reasonably acceptable due to a constant holdup ratio along the column height, which has been demonstrated for the direct contact evaporator [43-45].
- The heat losses from the direct contact column to the environment are ignored.

The simple energy balance for latent heat dominating in a three-phase direct contact condenser is:

$$Q_i = \dot{m}_c C_{pc} (T_{co} - T_{ci})_i = \dot{m}_{di} h_{fg} \quad (1)$$

And the volumetric heat transfer coefficient is obtained as:

$$U_{vi} = \frac{Q_i}{A \Delta Z_i (\Delta T_{lm})_i} \quad (2)$$

where $(\Delta T_{lm})_i$, Q_i , A , $\dot{m}_c$, $\dot{m}_{di}$ and ΔZ_i denote the log-mean temperature difference for each sub-volume, the total heat transfer rate for each sub-volume, the direct contact condenser cross-sectional area, the continuous phase mass flow rate, the dispersed mass flow rate that condenses at sub-volume and the height of a sub-volume, respectively.

Accurate prediction of U_{vi} through the direct contact heat exchange process requires an accurate knowledge of the temperature profile and consequently the temperature driving force.

This quantity, however, has a nonlinear distribution due to the backmixing and non-uniform

two-phase bubble size and distribution within the column. This can be solved by using the log-mean temperature difference. The log-mean temperature difference for our case may be written as:

$$\Delta T_{lm} = \frac{(T_{co} - T_{ci})}{\ln\left(\frac{T_{di} - T_{ci}}{T_{di} - T_{co}}\right)} \quad (3)$$

Combining Eqs. (1-3) yields the volumetric heat transfer coefficient:

$$U_{vi} = \frac{\dot{m}_c C_{pc}}{A \Delta Z_i} \ln \left[\frac{(T_{di} - T_{co})_i + \left(\frac{\dot{m}_{di}}{\dot{m}_c}\right) \frac{h_{fg}}{C_{pc}}}{(T_{di} - T_{co})_i} \right] \quad (4)$$

where C_{pc} , T_{di} , T_{co} and h_{fg} denote specific heat of the continuous phase, the temperature of the continuous phase at each individual sub-volume outlet, the temperature of the dispersed phase at each individual inlet sub-volume and the latent heat of condensation, respectively.

Among those quantities, both the dispersed phase mass flow rate at each sub-volume and the sub-volume dispersed phase inlet temperature, have to be estimated. The former can be calculated using following expression:

$$\dot{m}_{di} = \frac{Q_i}{h_{fg}} \quad (5)$$

where Q_i denotes the heat transfer rate at each sub-volume.

The dispersed phase inlet temperature of each sub-volume can be predicted using Antoine equation.

Figures 11 to 13 show the variation of the volumetric heat transfer coefficient for different initial dispersed phase temperatures and different mass flow rate ratios. A constant volumetric heat transfer coefficient along the direct contact column is clearly observed for the cases with a high dispersed phase mass flow rate, where the column completely flooded. This is regardless of how much the initial dispersed phase temperature or the continuous phase mass flow rate change. Of course, higher continuous or dispersed phase mass flow rate leads to a higher volumetric heat transfer coefficient, which is vividly shown by the figures.

As mentioned above, during flooding conditions, the temperature along the direct contact column tends to be uniform and nearly constant. Rapid increase in the continuous phase temperature along the direct contact condenser takes place as shown by the Figures (4-6).

This, of course, affects the volumetric heat transfer along the column and makes it nearly constant. This invariance of the volumetric heat transfer coefficient becomes clearest when it works under complete flooding conditions, as shown by Figures 11 to 13. This behaviour diminishes and the volumetric heat transfer might go to zero when the continuous phase completely stops flowing down, as indicated by Eq. (4). This is because of the continuous mass flow rate goes to zero. Therefore, it is important to set the direct contact condenser diameter at a point near to its flooding limit and adjusting the holdup ratio within the condenser to achieve a maximum heat exchange. The maximum error in the volumetric heat transfer coefficient was calculated and found $\pm 1.15\%$.

Fig. 11. Volumetric heat transfer coefficient of DCC during flooding inception for ($T_{di} = 40^{\circ}\text{C}$) and four different continuous and dispersed phase mass flow rates

Fig. 12. Volumetric heat transfer coefficient of DCC during flooding inception for ($T_{di} = 43.5^{\circ}\text{C}$) and four different continuous and dispersed phase mass flow rates

Fig.13. Volumetric heat transfer coefficient of DCC during flooding inception for ($T_{di} = 47.5^\circ\text{C}$) and four different continuous and dispersed phase mass flow rates

4. Conclusions

It was reported that flooding is the main shortcoming that can hinder performance of the three-phase spray column direct contact heat exchanger. In the present study, an experimental investigation of the heat transfer during the inception of flooding in a three-phase direct contact condenser was carried out.

Only the transient temperature distribution, the simple flooding map and the volumetric heat transfer coefficient were considered. The detailed study of this undesirable phenomenon may require a much more in-depth study. According to the data that was obtained, the time-dependent temperature distribution tends to be homogenised along the column after the onset of flooding. The visualisation study showed that flooding is preceded by the formation of large spherical, spherical-cap and finally slug bubbles. In addition, the dispersed phase mass flow rate required to create flooding is proportional to the continuous phase mass flow rate. The volumetric heat transfer coefficient increases towards the flooding inception and then decreases afterwards.

Nomenclatures

A cross-sectional area of column, m^2

a radius of two-phase bubble, m

226	C_{pc}	specific heat of continuous phase, $kJ/kg \cdot ^\circ C$
227	DCC	direct contact condenser
228	h_{fg}	latent heat of condensation, kJ/kg
229	$\dot{m}_d$	dispersed phase mass flow rate, kg/min
230	$\dot{m}_c$	continuous phase mass flow rate, kg/min
231	$\dot{m}_v$	dispersed (vapour) mass flow rate, kg/min
232	Q	heat transfer rate, kW
233	T	temperature, $^\circ C$
234	T_{c1-4}	temperature of continuous phase at f different locations along column, $^\circ C$
235	ΔT_{lm}	log-mean temperature difference, $^\circ C$
236	U	velocity of continuous phase, m/s
237	V_r	radial velocity component, m/s
238	V_θ	tangential velocity component, m/s
239	ΔZ	sub-height along column, m
240	Greek symbol	
241	β	half opening angle, <i>degrees</i>
242	Subscripts	
243	c	continuous phase
244	d	dispersed phase
245	i	initial, or location
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