Experimental Investigation of Sidewall Compression and Internal Contraction in a Scramjet Inlet

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In this paper, we present the results of an extensive measurement campaign to investigate the effects of external and internal sidewall compression and the variation of internal contraction on the performance and flowfield of a scramjet inlet. Experiments were conducted in the H2K wind tunnel of DLR, German Aerospace Center, in Cologne, Germany, at Mach 7. The performance was evaluated by static and total pressure ratios, kinetic energy efficiency, and mass capture ratios. The flowfield was analyzed using wall pressure distributions, pitot pressure, and Mach number profiles at the isolator interface to the combustion chamber and infrared thermography on the external ramps. The results show that the combination of a two-ramp inlet with external sidewall compression is not suitable for increasing the inlet's compression capability as it induces strong separation and vortex structures in the external part, which strongly increase spillage and impair the starting behavior. Thus, no significant increases in internal contraction are possible, which inhibits any gains in the performance of the inlet. With internal sidewall compression, strong increases of the pressure ratio can be achieved at the cost of total pressure losses. With internal sidewall compression as well as without sidewall compression, the inlet is still self-starting at internal contraction ratios well above the Kantrowitz limit.

Nomenclature

\begin{tabular}{ll}
\textit{A} & = area, \text{m}^2 \\
\textit{CR} & = overall contraction ratio \\
\textit{c_p, c_v} & = specific heat capacity at constant pressure/volume, \text{J} \cdot \text{kg}^{-1} \cdot \text{K}^{-1} \\
\textit{l} & = internal contraction ratio \\
\textit{M} & = Mach number \\
\textit{MCR} & = mass capture ratio \\
\textit{m} & = mass flow, \text{kg} \cdot \text{s}^{-1} \\
\textit{p} & = pressure, \text{Pa} \\
\textit{q} & = heat flux, \text{W} \cdot \text{m}^{-2} \\
\textit{R} & = specific gas constant for air, equal to 287.15 \\
& \text{J} \cdot \text{kg}^{-1} \cdot \text{K}^{-1} \\
\textit{Re} & = Reynolds number \\
\textit{r} & = recovery factor \\
\textit{St} & = Stanton number \\
\textit{T} & = temperature, \text{K} \\
\textit{t} & = time, \text{s} \\
\textit{α} & = heat transfer coefficient, \text{W} \cdot \text{m}^{-2} \cdot \text{K}^{-1}, or calibration factor \\
\textit{γ} & = ratio of specific heats \\
\textit{δ} & = ramp angle or sidewall-compression angle, \text{deg} \\
\textit{η_k} & = kinetic energy efficiency \\
\textit{λ} & = thermal conductivity, \text{W} \cdot \text{m}^{-1} \cdot \text{K}^{-1} \\
\textit{\Pi} & = static pressure ratio \\
\textit{\Pi_t} & = total pressure ratio \\
\textit{ρ} & = density, \text{kg} \cdot \text{m}^{-3} \\
\end{tabular}

Subscripts

\begin{tabular}{ll}
\textit{ex} & = isolator exit \\
\textit{ext} & = external sidewall compression \\
\textit{int} & = internal sidewall compression \\
\textit{L} & = lip \\
\textit{rec} & = recovery \\
\textit{SWC} & = sidewall compression \\
\textit{st} & = static \\
\textit{th} & = throat \\
\textit{∞} & = wind tunnel total condition \\
\end{tabular}

I. Introduction

In recent years, a focus in the development of advanced space access vehicles and high-speed transports has been on hypersonic airbreathing propulsion [1]. Especially, scramjets, in which, as opposed to regular ramjets, combustion takes place at supersonic speeds, are regarded as a key technology and constitute a vital part of many current and recent research projects, such as LAPCAT [2,3], the Hyper-X- and Hy-Tech-programs [4,5], LEA [6], and HIFiRE [7]. In Germany, a considerable part of scramjet research activities is concentrated in a research training group [8].

The inlet of a scramjet engine plays a major role for its overall performance and efficiency as it must ensure that the combustion chamber is supplied with a sufficient mass flow of air at the conditions required for stable and efficient combustion [9]. In two-dimensional, mixed-compression inlets, the airflow is compressed by oblique shocks from one or more ramps and the lips in both the external and internal parts of the inlet flow path. This is a very common type of inlet and has also been the subject of various other projects at the DLR, German Aerospace Center, in Cologne, Germany [10–15]. However, this type of inlet also brings several disadvantages. Because of small shock angles at high velocities, very long ramps that bring corresponding high structural mass are necessary to achieve the required compression ratios. Therefore, the focus of research efforts over the last years has shifted to three-dimensional (3D) inlets, in which compression occurs not only in vertical planes by the ramp and lip shocks but also in horizontal planes by the use of converging sidewalls. Heiser and Pratt [9] list several enhancements that can be achieved with 3D inlets. They allow for a more compact and thus lighter design and improved starting characteristics as well as less separation. Examples of 3D inlets are sidewall-compression inlets in

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which compression in horizontal planes only occurs by the forebody and the sidewall compression (SWC) takes place independently. This type of inlet was extensively examined by Holland [16,17] both computationally and experimentally. Goonko et al. [18,19], on the other hand, investigated 3D inlets with a single ramp and converging sidewalls. Further types of 3D inlets include streamline-traced inlets, like Busemann-type inlets or REST inlets with a transition from a rectangular capture area to an elliptical combustion chamber (Rectangular-to-Elliptical-Shape-Transitioning) [20,21].

The subject of this paper is the experimental analysis of the effects that the addition of sidewall compression and the variation of internal contraction has on the performance and flowfield of a two-dimensional, double-ramp mixed-compression inlet. This is also supposed to gather preliminary data for the future development of a new, completely three-dimensional inlet [22,23]. A similar approach has already been pursued by Gruhn and Gülhan [24] for the design of the inlet for the LAPCAT II configuration. A first study to increase the internal compression by additional sidewall compression in the internal part of inlet, i.e., the isolator, has been performed by Häberle and Gülhan [25]. The investigation showed that an increase in the pressure ratio of the inlet is possible but comes at the price of increased total pressure losses.

In contrast to this, the influence of additional sidewall compression by swept wedge inserts in the external part of the inlet is the subject of the current study. A comparison of the different types of inserts is shown in Fig. 1. There are two sets of inserts for each configuration so that the throat width can be reduced to two different values. Thus, two different sidewall-compression angles are achieved for both cases. The various configurations are described in detail in Sec. II.B.

Preliminary investigations for external sidewall compression showed, however, that the interaction of the sidewall shocks with the ramp shocks pushes the ramp shocks upward and causes a strong increase in spillage. This inhibits any gains from the additional compression, i.e., no increase in the pressure ratio but a strong decrease in the mass flow as well as lower pressure recovery. Computational fluid dynamics (CFD) simulations suggest that by elongating the cowl lip the goal of a higher pressure ratio with only small additional total pressure losses and no decrease of the mass flow can be achieved. Therefore, the inlet was modified so that it can be analyzed with different lip positions and consequently different internal contraction ratios. A detailed description of the various configurations is given in the next section.

Even in a two-dimensional (2D) inlet, the flowfield is very complex and contains various flow phenomena, such as shock–shock and shock–boundary-layer interactions, boundary-layer separation, and the formation of vortices. In the 2D configuration, the shock of the second ramp causes a separation bubble in the kink between the first and the second rams. At this point, transition from laminar to turbulent flow occurs as well. However, over the expansion surface between the second ramp and the isolator, the flow relaminarizes again [11,26]. Consequently, depending on the lip position, the lip shock will hit the boundary layer on the lower wall in a more or less relaminarized state. The shock–boundary-layer interaction at this position causes a large separation bubble, which was observed to be of a magnitude of about one-third of the height of the inlet throat [27]. Of course, the size of this separation bubble very much depends on the state of the boundary layer. A more relaminarized state would mean that the boundary layer is more likely to separate and that the separation would have a greater extent than if it is in a more turbulent state. Therefore, it is expected that less separation will occur on the lower wall in the throat section by the interaction with the lip shock when the lip is extended upstream. After this separation zone, the flow is assumed to be turbulent throughout the isolator.

While the elongation of the lip could lead to less separation on the lower wall, the movement of the second ramp shock underneath the cowl could cause more separation on the upper wall. As there already is a separation bubble generated at the upper wall by the separation shock of the separation bubble on the lower wall, there is the risk that these two shocks could create one very big separation zone on the upper wall, which could cause blockage of the inlet. To see if stable operation of the inlet is still possible when the second ramp shock is swallowed by the lip is one of the goals of this measurement campaign, as this could actually be a very useful feature since the hot mass flow that is generated in the separation zone induced by the shock–boundary-layer interaction of the second ramp shock could help with ignition in the combustion chamber.

Another issue is the corner flow. It is always very complex in a hypersonic inlet with the formation of vortices and shock–boundary-layer interactions on the sidewalls. This corner flow becomes even more complex by the sidewall-compression inserts. The additional shocks from the sidewalls create further interactions with the ramp shock, which generates a complex structure including the formation of a bridging shock wave and shock reflections toward the walls. Further vortices are induced and enhanced by the interactions. The transitional behavior is also strongly influenced by the sidewall shocks. A detailed description of the flow structure in a 3D inlet with swept sidewall leading edges is given by Goonko et al. [19].

These effects were investigated in an extensive wind tunnel campaign using the configurations mentioned previously, i.e., the different types of sidewall compression and the variation of the internal contraction by changing the lip position. Static and pitot pressure measurements were conducted to record wall pressure profiles and derive Mach number distributions in the inlets exit plane and to determine the performance of the various configurations. Furthermore, mass flows were measured with a conical throttle to calculate the mass capture. Infrared thermography was used to determine heat loads on the external ramps. This also gave some insight into the flow structure in this area. Further flow visualization for the cases without sidewall compression was achieved by shadowgraph imagery. The experimental setup, i.e., the wind tunnel facility and the inlet model with the various model configurations, is described in Sec. II. The measurement techniques are described in Sec. III, and the discussion of the results is given in Sec. IV.

II. Experimental Setup

A. Wind Tunnel and Test Conditions

All of the experiments presented in this paper have been conducted in the Hypersonic Wind tunnel H2K at the German Aerospace Center in Cologne, Germany. This facility is a blowdown wind tunnel using

![Fig. 1 Different configurations of sidewall compression.](image-url)
contoured axisymmetric nozzles designed to simulate Mach numbers of 5.3, 6.7, 8.7, and 11.2 at Reynolds numbers in the range of 2.5–20 \cdot 10^6 \text{ m}^{-1}, with total pressures up to 4 MPa and total temperatures up to 1000 K [28]. Variation of total temperature is achieved by an electrical heating system with up to 5 MW power. By varying the total temperature, different wall temperature ratios \( T_w / T_\infty \) can be achieved. The heating process is also used to dry the air in order to ensure that no condensation will occur in freestream. The size of the vacuum sphere is about 2000 m³, and the initial pressures in the vacuum sphere and test chamber are around 200 Pa. Thus, depending on the flow condition, test durations of up to 35 s can be achieved. A sketch of the H2K and its performance map are shown in Figs. 2 and 3. The test conditions correspond to those used in previous investigations [11,25] and are listed in Table 1. They were chosen to meet Mach and Reynolds number similarity for a flight in 30 km altitude at \( M = 7 \) with the consideration of a geometrical scaling factor of 1:1.5. At the beginning of the test run, the model is heated to ambient temperature, i.e., scaling factor of 1:1.5. At the beginning of the test run, the model is heated to ambient temperature, i.e., scaling factor of 1:1.5. 

This results in an actual Mach number of \( M = 7.02 \) for the condition used in the present study. The variation of the Mach number over the cross-section of the core flow is less than 0.5% and is shown in Fig. 4. Variation of total temperature during the tests was about \( \Delta T_\infty \approx \pm 7 \text{ K} \) or 1.4% and for total pressure was about \( \Delta p_\infty \approx \pm 7 \text{ kPa} \) or 1%.

### B. Inlet Model

The experiments were conducted with a two-dimensional two-ramp inlet with sidewalls, designed for a flight Mach number of \( M = 7.5 \). The ramp angles of the intake are \( \delta_1 = 9 \text{ deg} \) and \( \delta_2 = 20.5 \text{ deg} \) to the \( x \)-axis. The length of the inlet from the leading edge to the defined interface with the combustion chamber is \( L \approx 0.585 \text{ m} \). The capture area of the inlet is \( A_0 = 0.01 \text{ m}^2 \). The inlet was designed by the method of characteristics as described by Anderson [29]. A schematic sketch of the inlet is shown in Fig. 5, and a picture of the model with sidewall inserts mounted in the H2K is shown in Fig. 6. The inlet does not feature any starting mechanism and thus has to be self-starting.

The sidewall-compression inserts were designed to decrease the throat width of the inlet from 100 to 70 and 80 mm, respectively. The inserts for internal compression start at the end of the expansion surface at \( x = 410 \text{ mm} \) from the leading edge. The converging part of the inserts is 90 mm long, resulting in wedge angles of \( \delta_{\text{int,70}} = 9.5 \text{ deg} \) and \( \delta_{\text{int,80}} = 6.3 \text{ deg} \), respectively. The inserts for external sidewall compression have a sweep angle of 30 deg. The converging part of the inserts ends at the beginning of the expansion surface, i.e., at \( x = 342 \text{ mm} \). This yields sidewall-compression angles of \( \delta_{\text{ext,70}} = 3.5 \text{ deg} \) and \( \delta_{\text{ext,80}} = 2.7 \text{ deg} \). Because of constructive reasons, the width of the capture area had to be reduced by 1 to 99 mm. Figure 7 shows a sketch of the different sidewall-compression configurations and compression angles. For the inserts, the 70 mm cases are shown with solid lines, and the 80 mm cases are shown with dashed lines.

The position of the cowl lip is \( x_L = 180 \text{ mm} \) in the basic configuration. With the modification introduced for this study, it can be extended in steps by up to 40 mm and thus has a severe impact on the internal contraction ratio of the inlet. An overview of the different lip positions that were examined and the resulting internal contraction ratios \( I = A_0 / A_\infty \) and overall contraction ratio \( CR = A_0 / A_L \) is given in Table 2.

### Table 1  \hspace{1cm} Wind tunnel conditions

<table>
<thead>
<tr>
<th>Flow parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total temperature ( T_\infty ), K</td>
<td>500 ± 7</td>
</tr>
<tr>
<td>Total pressure ( p_\infty ), kPa</td>
<td>700 ± 7</td>
</tr>
<tr>
<td>Freestream Mach number ( M_\infty )</td>
<td>7.02 ± 0.04</td>
</tr>
<tr>
<td>Freestream pressure ( p_\infty ), Pa</td>
<td>169.1 ± 2</td>
</tr>
<tr>
<td>Freestream temperature ( T_\infty ), K</td>
<td>46.3 ± 0.1</td>
</tr>
<tr>
<td>Unit Reynolds number ( Re_\infty )</td>
<td>( (3.78 ± 0.1) \cdot 10^6 )</td>
</tr>
</tbody>
</table>

Since the inlet does not feature any starting mechanism, only self-starting configurations can ensure the safe operation of this inlet. A widely used criterion for the starting ability of scramjet intakes is the limit by Kantrowitz and Donaldson [30] in Eq. (2). It was originally derived for the one-dimensional flow through a supersonic diffuser and gives the maximum internal contraction ratio at which a normal shock in front of the inlet would still be swallowed and supersonic flow established.

\[
\delta_2 = 20.5 \text{ deg} \quad \text{to the} \quad x\text{-axis. The length of the inlet from the leading edge to the defined interface with the combustion chamber is} \quad L = 0.585 \text{ m. The capture area of the inlet is} \quad A_0 = 0.01 \text{ m}^2. \quad \text{The inlet was designed by the method of characteristics as described by Anderson [29]. A schematic sketch of the inlet is shown in Fig. 5, and a picture of the model with sidewall inserts mounted in the H2K is shown in Fig. 6. The inlet does not feature any starting mechanism and thus has to be self-starting.}

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\quad \text{The position of the cowl lip is} \quad x_L = 180 \text{ mm in the basic configuration. With the modification introduced for this study, it can be extended in steps by up to 40 mm and thus has a severe impact on the internal contraction ratio of the inlet. An overview of the different lip positions that were examined and the resulting internal contraction ratios} \quad I = A_0 / A_\infty \quad \text{and overall contraction ratio} \quad CR = A_0 / A_L \quad \text{is given in Table 2.}

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it is safe to say that an intake will start if its internal contraction is
isentropic contraction ratio, which is given in Eq. (3) [31]:

\[
\left[ \frac{A_L}{A_{in,\text{Kantrowitz}}} \right] = \left[ \frac{1}{M_L} \right] \left[ \frac{\gamma + 1}{\gamma - 1} \right]^{(\gamma+1)/\gamma} \frac{2 + (\gamma - 1)M_L^2}{\gamma + 1} \left( \frac{\gamma + 1}{2\gamma M_L^2 - (\gamma - 1)} \right)^{(\gamma+1)/(\gamma+1)}
\]

(2)

Once an inlet is started, the theoretical operational limit is the isentropic contraction ratio, which is given in Eq. (3) [31]:

\[
\left[ \frac{A_L}{A_{in,\text{Kantrowitz}}} \right] = \left[ \frac{1}{M_L} \right] \left[ \frac{2}{\gamma + 1} \right] \left( \frac{\gamma + 1}{2\gamma M_L^2} \right)^{(\gamma+1)/2}\left( \frac{\gamma + 1}{\gamma - 1} \right)^{\gamma+1}
\]

(3)

The Kantrowitz limit, however, is only a weak criterion. Although it is safe to say that an intake will start if its internal contraction is below the Kantrowitz limit, it might still start for higher values. Especially for 3D inlets, previous investigations found an improved starting behavior. Sun and Zhang [32] determined the empirical correlation in Eq. (4) for self-starting of 3D inlets from different studies for lip Mach numbers in the range \(1.65 \leq M_L \leq 4.68\):

\[
\left[ \frac{A_L}{A_{in,\text{empirical}}} \right] = 0.933 + \frac{M_L}{6.87} + \frac{M_L^2}{40.9}
\]

(4)

The internal contraction of the configurations that were tested in correlation to the previously mentioned criteria is graphically displayed in Fig. 8. In the graph, please not the way the various configurations and lip positions are indicated as described by the different legends. Please note that for better visualization the inverse \((A_L/A_{in})^{-1}\) of the internal contraction ratio is used at the ordinate. As the graph shows, several of the configurations are well above the Kantrowitz limit, and self-starting is not ensured.

III. Measurement Techniques

A. Flow Visualization

Four optical windows in the sidewalls allow optical access to the isolator part of the inlet model, as pointed out in Fig. 5. For flow visualization, a coincidence schlieren system is installed at the H2K wind tunnel. During this investigation, the system is used in shadowgraph mode.

B. Pressure Measurements

The inlet model is equipped with 42 static pressure ports along the centerline of the inlet, of which 25 are located on the lower wall and 17 are located on the upper wall. Furthermore, a pitot rake with five pitot tubes distributed over the height of the isolator is integrated on the centerline at the proposed interface of the isolator and the combustion chamber, i.e., at \(x = 585\) mm from the leading edge of the inlet. This pitot rake can be shifted 25 mm to the left and right of the centerline. A commercial Pressure Systems, Inc., 8400 system using a 32 psi module is used for the pressure measurements [33]. Both the results of the static and pitot pressure measurements are displayed as pressure ratios with regard to the freestream total pressure. The accuracies in the determination of the pressure ratios have been calculated to range from \(\pm 3.8\% - 5.6\%\) for static and \(\pm 3.4\% - 3.8\%\) for pitot pressures.

C. Mach Number Calculation

The results from the pitot pressure measurements are also used to calculate the Mach number of the flow at the entrance to the combustion chamber. For this procedure, one has to differentiate between supersonic and subsonic flow, which is determined by the ratio of the pitot and static pressure. For \(p_{\text{pitot}}/p_{\text{st}} \leq 1.8939\), the flow is supersonic, and for \(p_{\text{pitot}}/p_{\text{st}} > 1.8939\), it is subsonic [31]. In the first case, the Mach number can be calculated directly by...
In the second case, the Mach number has to be determined iteratively from Eq. (6):

$$M_{\text{Pitot}} = \left( \frac{\frac{p_{\text{Pitot}}}{p_\infty}}{\frac{p_\infty}{p_\infty}} \right) = 1 \left( \frac{(\gamma + 1) \cdot \frac{M_{\text{Pitot}}^2}{\gamma} - 2}{\gamma + 1} \right)$$  (6)

The determination of the accuracy for this procedure is quite difficult since it is not possible to measure the static pressure directly at the pitot tube. Instead, the pressure from the nearest wall pressure port has to be used. Assuming a variation of the static pressure of ±10% over the height of the isolator at the place of the pitot rake results in a relative error of the Mach number of ±5–6%. However, experience shows that, while this assumption is valid for most of the experiments, the variation of the static pressure can be significantly higher in some cases, especially when the isolator flow is already partially subsonic or a shock is present in between the pitot tube and the static pressure port that is being used.

D. Performance Parameters

The performance of the inlet is assessed by several parameters. These are the static pressure ratio $\Pi_{\text{ai}}$ as a means to judge on the inlet’s ability to compress the captured airflow as well as the total pressure recovery $\pi_t$ and the kinetic energy efficiency $\eta_{ke}$ to evaluate the efficiency of the inlet. The static pressure ratio is determined by the mean average of the wall pressure at the top and bottom walls at the location of the pitot pressure rake:

$$\Pi_{\text{ai}} = \frac{1}{2}(p_{\text{ai,top}} + p_{\text{ai,bottom}})$$  (7)

The total pressure recovery is calculated as the mass-flow-weighted average of the values at each measurement location $i$. To do so, the mass flow at each location is first determined by Eq. (8), and the total pressure recovery is then determined by Eq. (9):

$$M = p_{\text{ai,i}} \cdot A_i \cdot M_i \cdot \sqrt{\frac{\gamma}{R \cdot T_{\text{sto}}}} \cdot \left( 1 + \frac{\gamma - 1}{2} \cdot M_i^2 \right)$$  (8)

$$\pi_t = \frac{p_i}{p_\infty} = 1 \frac{1}{p_\infty} \frac{p_{\text{ai,i}}}{m_0} \left( 1 + \frac{\gamma - 1}{2} \cdot M_i^2 \right)^{\left( \gamma - 1 \right)/\gamma}$$  (9)

The kinetic energy efficiency can be determined directly from the total pressure ratio:

$$\eta_{ke} = 1 - \frac{2}{(\gamma - 1) \cdot \frac{M_{\text{in}}^2}{\gamma}} \left[ \left( \frac{1}{\pi_t} \right)^{\left( \gamma - 1 \right)/\gamma} - 1 \right]$$  (10)

E. Mass Flow Determination

During the tests, the inlet is mounted on a conical throttle, which is used to simulate the backpressure of the combustion chamber, and also serves as a mass flow meter. Assuming a one-dimensional flow and sonic condition in the throat of the throttle, i.e., the smallest cross-section area that is designated by index 4, the mass flow can be calculated with the procedure from Triesch and Krohn [34] by measuring the pressure in the settling chamber upstream of the throttle (position 3). The throat area is calculated from the position of the throttle cone and the geometric dimensions of the throttle by

$$A_4 = \frac{\pi \cdot s \cdot (r_3 + r_4)}{2}$$  (11)

The Mach number $M_3$ in the settling chamber can then be determined by iteratively solving Eq. (12):

$$A_3 = \frac{1}{M_3} \cdot \left[ 2 + (\gamma - 1) \cdot M_3^2 \right]^{\left( \gamma - 1 \right)/\gamma}$$  (12)

Using the static pressure $p_3$ upstream of the throttle, the mass flow can be calculated with the total temperature $T_{\text{sto}}$ of the wind tunnel flow and the Mach number $M_3$ by

$$m = p_3 \cdot A_3 \cdot M_3 \cdot \sqrt{\frac{\gamma}{R \cdot T_{\text{sto}}}} \cdot \left( 1 + \frac{\gamma - 1}{2} \cdot M_3^2 \right)$$  (13)

The static pressure $p_3$ is taken as the average value of four pressure ports located around the settling chamber of the throttle. With the mass flow going through the capture area $A_0$ of the inlet, which can be calculated by the freestream conditions of the wind tunnel, the mass capture ratio MCR can be determined by

$$\text{MCR} = \frac{\dot{m}_{\text{strat}}}{m_0} = \frac{\dot{m}_{\text{strat}}}{m_0}$$  (14)
F. Wall Heat Flux Measurements

During some experiments, the heat fluxes on the external ramps of the inlet are evaluated from infrared thermography. This not only gives information on heat loads and the selection of proper cooling mechanisms and materials but also allows insight into the flow structure on the surface and the state of the boundary layer. For the evaluation of the heat fluxes, the timewise development of the surface temperature distribution is recorded with an infrared camera. A FLIR Systems ThermaCAM SC-3000 is used for this [36]. Polyether ether ketone (PEEK) is used as material for the measurement surface. The recorded surface temperature distribution can then be used as the boundary condition for calculating the heat fluxes to the sidewall by evaluating the thermal energy balance of a solid volume:

\[ \rho(T) \cdot c(T) \frac{\partial T}{\partial t} = \nabla \cdot [\lambda(T) \nabla T] \]  \hspace{1cm} (15)

Assuming that lateral heat fluxes can be neglected due to the very low thermal conductivity of PEEK and accounting for temperature-dependent material properties, this transforms into the nonlinear one-dimensional heat equation normal to the wall,

\[ \frac{\partial T}{\partial t} = a(T) \cdot \frac{\partial^2 T}{\partial t^2} + b(T) \cdot \left( \frac{\partial T}{\partial t} \right)^2 \]  \hspace{1cm} (16)

with the thermal diffusivity

\[ a(T) = \frac{\lambda(T)}{\rho(T) \cdot c(T)} \]  \hspace{1cm} (17)

and

\[ b(T) = \frac{(\partial a(T)/\partial T)}{\rho(T) \cdot c(T)} \]  \hspace{1cm} (18)

Equation (16) is then solved by an explicit finite-difference scheme in order to calculate the temperature gradient in the normal direction inside the wall. From this, the wall heat flux can be calculated by the Fourier law:

\[ \dot{q}_W = (T_{y=0}) \cdot \frac{\partial T}{\partial y}_{y=0} \]  \hspace{1cm} (19)

The convective heat flux can then be calculated from the heat flux balance on the surface:

\[ \dot{q}_{\text{conv}} = \dot{q}_{\text{rad}} + \dot{q}_W \]  \hspace{1cm} (20)

where the radiative heat flux is calculated with the Stefan–Boltzmann law

\[ \dot{q}_{\text{rad}} = \varepsilon \cdot \sigma \cdot (T_{y=0}^4 - T_{\text{amb}}^4) \]  \hspace{1cm} (21)

assuming that the ambient temperature stays constant during the tests. A more detailed description of the evaluation method is given by Henckels and Gruhn [14]. Once the convective heat flux \( \dot{q}_{\text{conv}} \) has been determined, the dimensionless Stanton number can be calculated by Eq. (22), with the recovery temperature defined by Eq. (23). A recovery factor of \( r = 0.9 \) is used. The accuracy of the results is estimated to be within \( \pm 5\% \) for calculated heat fluxes and \( \pm 20\% \) for the Stanton number [27].

\[ St = \frac{\dot{q}_{\text{conv}}}{\rho_{\infty} \cdot u_{\infty} \cdot c_{p,\text{air}} \cdot (T_{\text{rec}} - T_{\infty})} \]  \hspace{1cm} (22)

\[ T_{\text{rec}} = \left( 1 + r \frac{\gamma - 1}{2} \cdot \frac{M_{\text{in}}^2}{\gamma} \right) \cdot T_{\infty} \]  \hspace{1cm} (23)

IV. Results

A. Starting Behavior and Flowfield on External Ramps

As mentioned in Sec. II, some of the configurations that were tested featured an internal contraction ratio that was significantly above the Kantrowitz criterion for self-starting. In contrast to the expectations, the configurations with additional external sidewall compression did not deliver an improved starting behavior. The inlet did not start for lip positions smaller than 360 mm for the case with 80 mm inserts and 375 mm for the case with 70 mm inserts. In contrast to that, all other investigated configurations did start; i.e., the inlet started during all test runs in which no inserts or additional internal compression was used.

The experimental results by themselves do not give any insight into the reason for this, as no flow visualization is available for these cases. However, related computational examinations by Nguyen et al. [26] show that from the interaction of the sidewall shocks with the shock of the second ramp a very large separation bubble in the kink between the first and the second ramps develops and reaches far downstream on the second ramp and only reattaches for a small area on the second ramp. Furthermore, there is strong formation of corner vorticity as well as a very large vortex in the center part of the inlet. These effects also create a very thick boundary layer on the second ramp and thus strongly increase the effective internal contraction of this inlet, which presumably also reduces its ability for self-starting. For detailed results, please refer to the corresponding paper [26].

However, as Fig. 9 shows, the measurements of the surface temperature and the subsequent derivation of the Stanton number distribution do support the observations from these simulations. As Hieberle and Gülhan [11] and Neuenhann and Olivier [37] showed for the 2D case, there is a separation bubble in the kink between the first and second ramps. The flow turns turbulent in the shear layer over the separation bubble, resulting in higher heat loads on the second ramp after the flow has reattached. Furthermore, the areas with higher Stanton numbers close to the sidewalls on the first ramp indicate the presence of corner vortices that are not very distinct. On the second ramp, the corner vortices are a bit more distinct. Furthermore, the streaklike pattern in the central part gives evidence that Görtler vortices are created.

In the case with 70 mm sidewall-compression inserts, these corner vortices are already stronger on the first ramp. The interaction with the second ramp shock strongly amplifies these vortices, resulting in a very high increase of the Stanton number in this area. Stainback and Weinstein [38] already noted the importance of heating caused by corner vorticity in the design of hypersonic vehicles. The results from the current measurement campaign show that in the case of 3D inlets even more attention has to be paid to this aspect.

In the central part of the second ramp, the IR measurements show signs of larger secondary vortices that are induced by the interaction of the core flow and the corner flow. However, it is difficult to reliably conclude what the flow structure really looks like. There are also significant differences in the CFD calculations [26]. It is especially unknown how the sidewall compression influences the transitional behavior. Overall, because of the flow structure on the external ramps and the consequential poor starting behavior, the combination of a double-ramp inlet and sidewall compression seems very unfavorable.

B. Performance

Figure 10 shows the comparison of various performance parameters for all the different configurations and lip positions. The mass capture ratios in Fig. 10a show that a strong reduction of the spillage mass flow can be achieved by the adaption of the lip position. In the case without sidewall inserts, it is possible to capture almost the full mass flow through the capture area when the lip is extended to \( x_L = 340 \) mm. With external sidewall compression, the MCR can...
also be increased, but it is limited by the point at which the inlet does not start anymore. For additional internal contraction, the results look very similar to those of the 2D case. There is no difference between the two different insert configurations, but the values are slightly lower than in the 2D case. It is believed that this is due to an upstream effect of the internal sidewall-compression wedges along the upper wall. This can also be seen in the wall pressure distribution in Fig. 11 and was already noticed by Häberle and Gülhan [25].

The comparison of both the static and total pressure ratios of the different configurations is shown in Figs. 10c and 10d. It illustrates that a considerable increase of the static pressure ratio by the elongation of the lip and consequential higher MCR by itself cannot be achieved; the static pressure ratio is only increased by about 11%. On the other hand, in combination with additional sidewall compression, much higher pressure levels can be achieved. Again, the starting behavior of the cases with external sidewall compression acts as a limiting factor. In the case of additional internal sidewall compression with a reduction of the throat width to 70 mm, the strongest gains in the static pressure level can be achieved, yielding an increase of around 56%. For the 80 mm inserts, the increase with about 21% is more moderate, as expected.

Regarding the total pressure ratio in Fig. 10d the results are as expected. With sidewall compression and consequent total pressure losses from the sidewall shocks, the total pressure ratios are always lower than in the 2D case. The total pressure ratio decreases with increasing sidewall-compression angle and corresponding stronger shocks and higher total pressure losses. The total pressure recovery increases when the cowl lip is extended to the front. The increases for all configurations with sidewall compression are lower than in the 2D case. This is presumably due to the second ramp shock going underneath the cowl and interacting with the lip shock before it hits the cowl.
C. Flowfield

1. Variation of Internal Contraction Without Sidewall Compression

In this section, the influence of the lip length on the flowfield is analyzed more closely. By elongating the lip to the front, the internal contraction ratio is significantly increased, as described in Sec. II. Only the 2D configuration is used for this, because only for this configuration is a full set of data for all lip lengths available, and it is possible to take shadowgraph images. Figure 12 shows the comparison of the static pressure distribution along the lower and upper walls of the inlet for different lip positions for the configuration without sidewall-compression inserts as well as shadowgraph images for lip positions $x_L = 380$ mm and $x_L = 340$ mm. The shadowgraph images of the first window show the changes in the shock structure, when the lip is extended upstream. In the original lip position, the second ramp shock passes clearly ahead of the cowl lip. The interaction of the lip shock with the ramp boundary layer causes a separation bubble in the throat in between $x = 390$ and 410 mm. The separation shock in turn causes the boundary layer on the upper wall...
to separate at about $x = 420$ mm. The size of this separation zone is much smaller, however. The reattachment shock of the separation bubble on the lower wall is reflected in a shock-train-like structure throughout the length of the isolator. The separation and reattachment shocks of the separation zone on the upper wall are much weaker and are not visible anymore after interacting with the ramp boundary layer.

If the lip is elongated by more than 5 mm, the shock from the second ramp is swallowed by the cowl lip. It first interacts with the lip shock and then hits the upper wall at about the place of the second pressure port, i.e., at $x = 375$ mm. The shock produces only a small separation zone at the upper wall. The separation and reattachment shocks only interact with the ramp boundary layer after the separation region that is caused by the interaction with the lip shock. This separation region still starts at about the same location $x = 390$ mm but is much smaller, only extending to about $x = 400$ mm. In this case, the separation shock of this separation zone appears much weaker, and after it is reflected by the upper wall and hits the lower wall again, it cannot be seen anymore in the shadowgraph images. On the other hand, both the reattachment and separation shocks of the separation zone induced by the second ramp shock can be seen to be reflected throughout the length of the isolator in the same way as is the case with the reattachment shock from the separation bubble on the lower wall for the 380 mm lip. The shock structure is also still at almost the same position, and therefore also no shift in the pressure profiles along both the upper and low walls can be noted.

Although, as shown in Fig. 10a, the elongation of the lip increases the mass flow by almost 30%, an overall increase of the static pressure level from the extension of the lip cannot be observed. There is a strong pressure rise in the leading part of the cowl for those configurations in which the second ramp shock hits the cowl. The changes in the shock structure also cause an increase of the pressure along the lower wall in the throat section. However, in the rear part of the isolator, the flow gets more homogeneous again, and the pressure characteristics of the different configurations become more alike.

The changes in the pitot pressure that are induced by the extension of the lip are much greater. As Fig. 13 shows, there is a strong increase in the pitot pressure in the center part of the isolator, while the differences are small at the pitot tubes close to the wall. It is interesting to note that the location at which the peak pitot pressure is reached changes several times when the lip is extended further (this is also true for the configurations not displayed here). The change of the lip position from 360 to 350 mm causes a strong increase in the pitot pressure in the upper half of the isolator flow, while the pressure falls in the lower part. The further extension to 340 mm shows the exact opposite: a strong increase in the lower and center parts of the flow but a decrease in the upper part. This is a sign of lateral movements of the shock structures in the isolator that are due to the change of the location of the lip shock and the resulting change in the interaction with shocks from the separation bubble on the ramp expansion surface.

2. Comparison of Different Insert Configurations for Basic Configuration with $x_L = 380$ mm

Figure 11 shows the static pressure distribution along the centerline of the upper and lower walls up to the defined interface of the inlet and the combustion chamber of the different insert configurations for a lip position of $x = 380$ mm. The additional external sidewall compression does not yield significant changes in the pressure levels. There is also no significant difference between the 70 and 80 mm cases. However, the inserts for internal sidewall compression cause a strong pressure rise in the isolator part in the region from $x = 490$–550 mm by a factor of up to 3 for the 70 mm case. As previous investigations showed that the isolator was designed too long by about one-third [11], the section of the isolator showing the strongest pressure rise would actually be the one where the interface of the combustion chamber should be, and consequently these types of inserts would cause a pressure rise in the area where fuel injection and ignition should take place. Downstream of this area, however, the pressure level aligns with that of the 2D case again.

The current examinations also show a strong upstream effect for the cases with internal sidewall compression. The pressure in the throat area, i.e., for $0.38 < x < 0.44$, is strongly increased as compared to the other configurations. This pressure rise starts clearly upstream of the leading edge of the sidewall inserts at $x = 0.41$. This is due to the interaction of the shocks that are induced by the sidewall-compression wedges with the boundary on both the upper and lower walls. This effect has also already been observed in previous investigations [11]. The interaction of these shocks with the boundary layer on the lower wall causes the separation bubble on the expansion surface of the lower wall to move upstream, which can be seen by the pressure rise in the region around $x = 0.37$.

Consequently, the separation shock hits the upper wall further upstream as well, thus causing the strong increase in pressure at the first pressure port of the upper wall.

Overall, it is expected that the addition of sidewall compression creates a more inhomogeneous flowfield by introducing more three-dimensional effects to flow. However, the plots of pitot pressure and Mach number distribution over the isolator height in Fig. 14 indicate that in the $x$-direction the flow is more homogeneous than in the 2D case. The variation of both pitot pressure and Mach number over the isolator height is much smaller. The configuration of 70 mm internal compression shows the smallest variation of both parameters. Furthermore, the Mach number is lowest for this configuration. For
Fig. 14  Mach number and pitot pressure distributions over the isolator height for lip position $x_L = 380$ mm.

Fig. 15  Comparison of centerline and off-centerline Mach number and pitot pressure profiles for external (top) and internal (bottom) sidewall compression.
the pitot pressure, the peak value is much lower, but close to the walls, the pitot pressure is higher than for the 2D case. The profiles for external sidewall compression are more similar to the 2D case. Especially, the Mach number distribution is almost equal.

The pitot pressure distribution shows a lower level in the center part of the isolator, but the characteristics are very similar. Like for the wall pressure distribution, the profiles for the two different throat widths for external sidewall compression are almost identical.

To check, how the 3D inserts influence the flow structure in the spanwise direction, the pitot rake was moved 25 mm off the centerline toward the wall. Figure 15 shows the comparison of Mach number and pitot pressure profiles on and off the centerline for the different insert types.

For external sidewall compression, it is very interesting to note that the flow becomes more homogeneous as the sidewall compression is increased. The discrepancies between the profile on and off the centerline are largest for the 2D case. For 80 mm compression, the off-centerline profile is nearly identical to the 2D case but does not differ as much from the centerline profile. Finally, for 70 mm, there are only very small differences between the two different locations of the pitot rake. Since the converging part of the external sidewall inserts already ends in the area of the cowl lip, it appears that the 3D shock structures that are induced by the inserts do not protrude into the isolator and that the flow is very much homogenized.

Opposed to that, for internal compression, the 3D shock structures are only introduced in the isolator part of the inlet. Therefore, the flow is still very nonuniform in the z-direction at the interface to the combustion chamber (where the pitot rake is located). For the 80 mm inserts, the tendency is similar to the one in the 2D case: The pitot pressure at the top pitot tube is increased, while it decreases in all other places, although the quantitative changes are smaller. Reducing the width to 70 mm results in much larger changes. The pitot pressure rises at all pitot tubes, and the highest value is now measured at the pitot tube closest to the bottom, whereas it is at the topmost one for the other two configurations. However, the differences of the pitot pressure at the different pitot tubes are now smaller.

3. Influence of Sidewall Compression at Increased Internal Contraction with \( x_L = 360 \) mm

To see the influence of the lip position, and consequently higher captured mass flow, on the internal flowfield of the various configurations, the wall pressure distribution of the different configurations for a lip length of \( x_L = 360 \) mm is displayed in Fig. 16.

![Fig. 16 Wall pressure distribution for different insert types for lip position \( x_L = 360 \) mm.](image)

![Fig. 17 Mach number (left) and pitot pressure (right) profiles for lip position \( x_L = 360 \) mm.](image)
The profiles for all configurations are quite similar to the reference configuration with $x_L = 380 \text{ mm}$. There is a strong pressure rise in the central area of the isolator for both cases with internal sidewall compression. The peak value at the cowl is moved upstream. For $b_{in} = 80 \text{ mm}$, the same can be observed on the ramp side. Since, because of constructive reasons, there are no pressure ports along the lower wall in the central region of the isolator (in between $470 \text{ mm} < x < 530 \text{ mm}$), it is not known whether this also occurs for the 70 mm case. In the throat section, an upstream effect similar to the case with $x_L = 380 \text{ mm}$, i.e., a significant pressure rise, which is due to the interaction that was already mentioned previously, is present.

For the case of external sidewall compression, a pressure rise can be noted in the throat section. The increase of pressure along the upper wall in the downstream part of the isolator is more distinct than for $x_L = 380 \text{ mm}$, but overall there are no significant changes.

Regarding the pitot and Mach number profiles, which are shown in Fig. 17, also for this lip length, the flow seems to become more homogeneous by the addition of internal sidewall compression. The Mach number and pitot pressure levels of the 80 mm case are lower than for the 70 mm case, which corresponds to the observations made from the wall pressure distribution. Again, the highest values are achieved for the 2D case; however, the location of the peak value moves to the lower wall, corresponding to the movement of the shock structure by the elongation of the lip. In the case of external compression, the profiles now differ substantially from the 2D case.

V. Conclusions

The results of this experimental campaign give lots of insight into aspects that have to be considered in the design of three-dimensional inlets. The results show that combining swept external sidewall compression to a two-ramp inlet has very unfavorable effects. For once, the interaction of the sidewall shocks pushes the ramp shocks upward and causes a strong increase in spillage. This inhibits an increase of the compression ratio by the additional sidewall compression. Furthermore, the sidewall shocks induce corner vortices of which the interactions with the second ramp shock create vast separation in the kink in between the two ramps and on the second ramp. This has a strong impact on the starting behavior; i.e., the inlet does not start anymore when the lip is substantially elongated upstream to catch more mass flow to counterbalance the increased spillage, and the internal contraction is increased accordingly.

Opposed to this, a significant increase of the compression ratio of the inlet can be achieved by additional internal sidewall compression. The static pressure could almost be tripled in certain areas, and the overall static pressure ratio could be raised by almost 70%. At the same time, it is possible to capture significantly more mass flow by adapting the lip position. In this configuration, as well as in the 2D case, it is possible to elongate the lip quite far upstream without inhibiting the self-starting of the inlet. The internal contraction ratios in these cases are well above the Kantrowitz criterion. Furthermore, the movement of the second ramp shock underneath the lip and its interaction with the boundary layer on the cowl and the separation caused there do not have negative an influence on this.

Once again, the results also show that the Kantrowitz limit is only a weak criterion on which to judge the starting behavior of hypersonic inlets. Overall, it seems that it is more influenced by the flow structure and the phenomena occurring in the respective configurations such as vortex structures, shock–shock and shock–boundary layer interactions, and separation than by the internal contraction.

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