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The lateral instability problem in electrostatic comb drive actuators: modeling and feedback control

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Abstract
Comb drives inherently suffer from electromechanical instability called lateral pull-in, side pull-in or, sometimes, lateral instability. Although fabricated to be perfectly symmetrical, the actuator’s comb structure is always unbalanced, causing adjacent finger electrodes to contact each other when voltage–deflection conditions are favorable. Lateral instability decreases the active traveling range of the actuator, and the problem is typically approached by improving the mechanical design of the suspension. In this paper, a novel approach to counteracting the pull-in phenomenon is proposed. It is shown that the pull-in problem can be successfully counteracted by introducing active feedback steering of the lateral motion. In order to do this, however, the actuator must be controllable in the lateral direction, and lateral deflection measurements need to be available. It is shown herein how to accomplish this. The experimentally verified dynamic model of the comb drive is extended with a lateral motion model. The lateral part of the model is verified through experimental results and finite element analysis and is hypothetically extended to accommodate both sensor and actuator functionalities for lateral movement. A set of simulations is performed to illustrate the improved traveling range gained by the controller.

1. Introduction

Electrostatic actuators have an important role in MEMS technology. Compared with the other types of micro actuators [1, 2], electrostatic actuators generate relatively modest force (several µN). However, their corresponding low power consumption makes them a frequent choice of MEMS designers [3]. One of the most common electrostatic actuators is the comb drive, which exhibits an interesting and useful property in that the generated force does not depend on actuator position (deflection), but only on the square of the applied voltage. The actuator mathematical model, developed in one degree-of-freedom (DOF), is relatively simple [4, 5], and driving the actuator is quite straightforward.

The main issue of the comb drive design is achieving large deflections while minimizing the actuation voltage, resulting in a small deflection-to-size ratio of the actuator. These requirements are typically matched by balancing the design of actuator’s suspension and varying the size of the force-generating comb structure. The comb drives, however, suffer from a electromechanical instability called lateral or side pull-in, or lateral instability. Electrostatic forces, perpendicular to the desired movement of the actuator, can get unbalanced causing the neighboring electrodes to contact each other when the voltage–deflection conditions are favorable. Weak suspensions and large forces, designed to achieve large traveling ranges, increase this problem even more. An example of the comb fingers in the state of lateral instability is shown in figure 1.
The lateral instability occurs when the electrostatic stiffness transverse to the axial direction of motion exceeds the transverse mechanical stiffness of the suspension [6, 7]. Therefore, the most common way to avoid it is by increasing the transverse stiffness of the suspension [1, 8, 9].

As an addition to the commonly utilized transverse spring redesign, we propose a novel approach relying on the use of feedback control to counteract the lateral instability. A requirement for doing so is to have a lateral motion sensing capability and an appropriate model of the device for the subsequent control system design. The introduction of the lateral feedback may impact the design of the comb drive, mitigating the requirements on the suspension, lowering the actuation voltage and, therefore, decreasing the ratio of the size of the actuator and achievable deflection. In order to do this, we have to establish a suitable model for lateral stability analysis, and then design an appropriate control system.

In section 2, the existing comb drive, fabricated using deep reactive ion etching (DRIE), with a well-developed, experimentally verified mathematical model in one degree-of-freedom (DOF) [10], is extended to include the asymmetrical lateral DOF. The device is shown in figure 2(a). The parameters of the lateral DOF model are determined through the combined finite element analysis (FEA) and static experimental results in section 3. In section 4, following the model verification, we hypothetically extend the structure of the device with both sensor and actuator functionalities for lateral movement as shown in figure 2(b). These additional features enable the design of the controller for lateral motion, described in section 5. A discussion on the implementation issues concludes this paper.

2. Mathematical model

The mathematical modeling presented in this section is done for the actual actuator shown in figures 1 and 2(a). First, modal FEA is conducted to determine the modes of the device that participate in the motion so as to actually define the structure of the mechanical model. This exercise is followed by derivation of the electrostatic forces.

2.1. Structure of the mechanical model

The flexible comb drive is a Lagrangian system and, as a flexible structure, can be generally modeled as an infinite set of second-order differential equations. For practical purpose, however, it is necessary to include only the first few significant modes in the model, making it finite dimensional. The dynamic model of such a structure is generally written as

\[ \mathbf{M} \ddot{\mathbf{q}} + \mathbf{D} \dot{\mathbf{q}} + \mathbf{K} \mathbf{q} = \mathbf{F}_N, \]

where \( \mathbf{M} \) is the modal inertia matrix, \( \mathbf{D} \) is the modal damping matrix, \( \mathbf{K} \) is the modal stiffness matrix and \( \mathbf{F}_N \) contains modal forces acting on the system, \( \mathbf{q} = [q_1, q_2, \ldots, q_j]^T \) is a vector containing generalized modal coordinates, and \( \dot{\mathbf{q}} \) and \( \ddot{\mathbf{q}} \) are its first and second derivatives, respectively. System contains \( j \) modes.

In order to determine the parameters \( \mathbf{M} \) and \( \mathbf{K} \) of the model (1), both static and modal FEA were conducted. The FEA results can be used to fill the \( \mathbf{M} \) and \( \mathbf{K} \) matrices with the parameters. The mode values and the corresponding parameters are shown in table 1. A detailed discussion on experimental values of the first mode is
Table 1. FEA-computed values for the modes, effective masses and stiffness parameters.

<table>
<thead>
<tr>
<th>Mode (i)</th>
<th>Value</th>
<th>( m_{ii} )</th>
<th>( k_{ii} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1692 Hz</td>
<td>( 7.75 \times 10^{-8} ) kg</td>
<td>( 1.08 ) N m(^{-1}) Along x</td>
</tr>
<tr>
<td>2</td>
<td>3744 Hz</td>
<td>( 3.85 \times 10^{-15} ) kg m(^2)</td>
<td>( 2.13 \times 10^{-7} ) Nm rad(^{-1}) Around y</td>
</tr>
<tr>
<td>3</td>
<td>5427 Hz</td>
<td>( 1.39 \times 10^{-15} ) kg m(^2)</td>
<td>( 1.62 \times 10^{-6} ) Nm rad(^{-1}) Comb around x</td>
</tr>
<tr>
<td>4</td>
<td>6812 Hz</td>
<td>( 1.88 \times 10^{-15} ) kg m(^2)</td>
<td>( 3.45 \times 10^{-6} ) Nm rad(^{-1}) Around z</td>
</tr>
</tbody>
</table>

2.2. General model: assumptions and structure

To begin with, it will be assumed that the second and third modes do not participate in the motion along the x-axis nor around the z-axis. For the purpose of developing the simple model, we need a two degree-of-freedom (DOF) model, containing the first and the fourth mode. Additional modes can, however, influence the motion of interest, and we will discuss this issue later in the paper.

With only first and fourth DOFs left, i.e. \( \mathbf{F}_N = [n_1 \quad n_4]^T = [F_1 \quad M_4]^T, \mathbf{q} = [q_1 \quad q_4]^T = [x \quad \theta]^T \) from (1), the mathematical model \([1]\) is reduced to

\[
m_{ii} \ddot{x} + d_{ii} \dot{x} + d_{ii} \dot{\theta} + k_{ii} \ddot{\theta} = F_i
\]

and

\[
J_{ii} \ddot{\theta} + d_{ii} \dot{\theta} + k_{ii} \ddot{\theta} = M_i ;
\]

where \( m_{ii} \) and \( J_{ii} \) are the effective moving mass along the x-axis, and the effective moment of inertia around the z-axis, respectively. Notations \( d_{ii} \) and \( d_{ii} \) describe damping, and \( k_{ii} \) and \( k_{ii} \) are the stiffnesses along the x-axis and around the z-axis, respectively.

It can be seen that \( \theta \) is small during the operation of the device. In other words, the y-direction movement of the comb structure is a way smaller than the radius of rotation, i.e. \( y \ll L_A \). Hence, we can approximate \( tg \theta = y/L_A \approx \theta \), modifying (3) as

\[
\left( J_{ii} / L_A \right) \ddot{\theta} + (d_{ii} / L_A) \dot{\theta} + (k_{ii} / L_A) \theta = -F_Y L_A \quad (4)
\]

and, after dividing (4) by \( L_A \),

\[
\left( J_{ii} / L_A^2 \right) \ddot{\theta} + (d_{ii} / L_A^2) \dot{\theta} + (k_{ii} / L_A^2) \theta = -F_Y . \quad (5)
\]

Equations (1) and (5) represent the dynamics of the comb drive actuator in a two DOF.

2.3. Electrostatic model: actuator and lateral instability

The structure of the comb drive actuator is shown in figure 4. The virtually symmetrical comb drive actuator is made laterally unbalanced by introducing \( \Delta d \). The models of force generation in both x- and y-directions need to be determined.

The capacitances \( C_F \) and \( C_B \) as a function of the variables \( x \) and \( y \) are given as

\[
C_F(x, y) = N \epsilon_0 T(x + x_0) \left( \frac{1}{d - \Delta d - y + \frac{1}{d + \Delta d + y}} \right), \quad (6)
\]

\[
C_B(x, y) = N \epsilon_0 T(-x + x_0) \left( \frac{1}{d - \Delta d - y + \frac{1}{d + \Delta d + y}} \right), \quad (7)
\]

where \( C_F \) and \( C_B \) are forward- and backward-actuating capacitances, \( N \) is the number of finger electrodes of each comb.
drive, \( \varepsilon_0 \) is the permittivity of the vacuum, \( T \) is the thickness of the structure and \( x_0 \) is the initial overlapping between the fingers.

The force in the \( x \)-direction, \( F_x \), is given as \[ F_x = \frac{1}{2} \frac{\partial C_F}{\partial x} V_F^2 + \frac{1}{2} \frac{\partial C_B}{\partial x} V_B^2 \tag{8} \]

where \( V_F \) and \( V_B \) are the forward- and backward-driving voltages, respectively.

Introducing (6) and (7) into (8), the resulting force in the \( x \)-direction is given as
\[ F_x = \frac{1}{2} N \varepsilon_0 T \left[ \frac{1}{d - \Delta d - y} + \frac{1}{d + \Delta d + y} \right] (V_F^2 - V_B^2). \tag{9} \]

Similarly, the lateral force, \( F_y \), is given as
\[ F_y = \frac{1}{2} \frac{\partial C_F}{\partial y} V_F^2 + \frac{1}{2} \frac{\partial C_B}{\partial y} V_B^2 \tag{10} \]

and, after substituting (6) and (7) into (10) the lateral force can be expressed as
\[ F_y = \frac{1}{2} N \varepsilon_0 T \left[ \frac{1}{(d - \Delta d - y)^2} - \frac{1}{(d + \Delta d + y)^2} \right] \times \left[ x_0 (V_F^2 + V_B^2) + x (V_F^2 - V_B^2) \right]. \tag{11} \]

Note that when \( \Delta d = 0 \) the lateral force exists only if \( y \) is not zero.

Equations (1) and (5), together with (9) and (11), represent the dynamic model of the actuator in a two DOF. The model is nonlinear and coupled through the generation of the electrostatic forces in (9) and (11).

3. Model verification and refinement

The model developed in section 2 presents an overview of the dynamic behavior of the device. However, it does not accurately represent actual device. Therefore, several refinements are needed, and some parameters need to be determined. Interdigitated capacitances are assumed to be larger due to the finite aspect ratio and fringing fields. Consequently, the force can be modified by a constant, determined from the experimental results. A set of FEA computations was conducted to determine the aspect ratio and contribution of the fringe fields, and to determine the \( y \)- and \( z \)-dependence of the capacitance. The lateral ‘unbalance’ coefficient, \( \Delta d \), was determined from the experimental results, and the model was refined into its final form.

The device described in this paper is fabricated in DRIE. So far, it has been assumed that the sidewalls of the device are perfectly perpendicular to the substrate (see the dashed lines in figure 5). However, in reality, this is not true. As illustrated in figure 5, the aspect ratio is not infinite but has a finite value. As such, it modifies the interdigitated capacitance and, consequently, the electrostatic force. The increase of the value of the capacitance as a function of the angle \( \alpha \) is given as the capacitance ratio \[ \frac{C(\alpha)}{C(0)} = \frac{d}{d - 2T \tan \alpha} \ln \frac{d}{d - 2T \tan \alpha} \tag{12} \]

\[ \lim_{\alpha \to 0} [C(\alpha)/C(0)] = 1. \tag{13} \]

Ratio (12), for the device addressed in this paper, is given in figure 6. Note that due to the high aspect ratio, i.e. \( T = 75 \mu m \) and \( d = 2.5 \mu m \), the actual value of the capacitance significantly increases even for small sidewall angles.

Besides the finite aspect ratio of the structure, fringing fields also affect the interdigitated capacitance. In order to
describe modification of the electrostatic force in terms of the modified capacitance, a constant $\eta$ is introduced. That is

$$F_m = \frac{1}{2} \frac{\partial (\eta C_0)}{\partial x} V^2 = \frac{1}{2} \eta \frac{\partial C_0}{\partial x} V^2 = \eta F_0.$$  \hspace{1cm} (14)

where $\eta$ is the modification of the capacitance according to equation (12), and is shown in figure 6.

Applying (14) to the model in (2) and (4) yields

$$m \ddot{x} + d_x \dot{x} + k_x x = \eta F_x$$  \hspace{1cm} (15)

$$(J_z / L_A) \dddot{y} + (d_y / L_A) \ddot{y} + (k_y / L_A) y = -\eta F_y L_A.$$  \hspace{1cm} (16)

Assuming steady-state conditions, the model in (15) and (16) reduces to

$$k_x x = \eta F_x = k_{es} V_x^2$$  \hspace{1cm} (17)

$$(k_y / L_A) y = \eta F_y L_A.$$  \hspace{1cm} (18)

Both experimentally and analytically determined curves of (17) are plotted in figure 7 [10]. The theoretically calculated value $k_{es}/k_x$ fits for 0.039 $\mu$m V$^{-2}$. Experimental results, however, show that its value is 0.081 $\mu$m V$^{-2}$. The ratio of these two functions is $\eta = 2.07$ (see figure 6). It is important to mention that the experimental observation shows that the influence of the lateral motion over the majority of the traveling range of the actuator is negligible. Lateral movement does not affect the force in the $x$-direction. Lateral motions become visible when the applied voltage approaches the value of the pull-in voltage. This assumption allows us to determine $\eta$ independently from (17) and figure 7.

In order to determine and separate the contribution of the finite aspect ratio and fringing fields to the sensing capacitances, an electrostatic FEA was conducted. The aspect ratio was varied, and the increase in the capacitance was observed. When the capacitance increase was 2.07 times its original value (6)–(7), the angle $\alpha$ was computed to be $0.786^\circ$. In addition, the majority of the capacitance increase was due to the finite aspect ratio, and only 2–3% was attributed to the fringing fields.

Keeping the angle $\alpha$ constant (i.e., $0.786^\circ$), another FEA investigation was conducted to observe the influence of the out-of-plane motion on the capacitance. Both $y$ and $z$ were varied over the interval of interest, and the results are shown in figure 8.

The maximal change of sensing capacitance due to the $z$-motion inside $|y| < 0.1 \mu$m, where controller is expected to stabilize lateral motions, is 5% for the worst case, i.e. $z = 2.5 \mu$m. Capacitance change due to the lateral motion is within 10–15%. In order to distinguish between these two motions proper connection of the sensing structure, shown in figure 11, ensures that the lateral motion is sensed differentially whether out-plane influence is rejected being a common signal component of the motion [11].

With $\eta$ known, the remaining parameter to be determined is $\Delta d$ from (11), which affects $F_y$ in (18). The experimental results of the lateral part of the model are shown in figure 9. The last stable voltage and lateral deflection before pull-in were 8.96 V and 0.65 $\mu$m, respectively. In order to determine the value of $\Delta d$ from (11), which implicitly affects $F_y$ in (18), a set of simulations in MATLAB/SIMULINK was conducted. The applied voltage was increased gradually, as it was in the actual experiment. The value of $\Delta d$ was 0.31 $\mu$m when pull-in occurs at 8.96 V.

3.1. Model summary and characteristics

Summarized, the model looks like

$$m_x \ddot{x} + d_x \dot{x} + k_x x = \eta F_x$$  \hspace{1cm} (19)

$$(J_z / L_A^2) \dddot{y} + (d_y / L_A^2) \ddot{y} + (k_y / L_A^2) y = -\eta F_y.$$  \hspace{1cm} (20)
where

\[
F_s = \frac{1}{2} N \varepsilon_0 T \left( \frac{1}{d - \Delta d - y} + \frac{1}{d + \Delta d + y} \right) (V_f^2 - V_b^2)
\]

and

\[
F_x = \frac{1}{2} N \varepsilon_0 T \left[ \frac{1}{(d - \Delta d - y)^2} - \frac{1}{(d + \Delta d + y)^2} \right]
\times \left[ x_0 (V_f^2 + V_b^2) + x (V_f^2 - V_b^2) \right],
\]

and the associated parameters are given as follows: \( m_1 = 7.75 \times 10^{-4} \text{ kg} \), \( J_1 = 1.88 \times 10^{-13} \text{ kg m}^2 \), \( k_i = 1.08 \text{ N m}^{-1} \), \( k_o = 3.45 \times 10^{-6} \text{ Nm rad}^{-1} \), \( L_A = 246 \mu \text{m} \), \( d_i = 8 \times 10^{-5} \text{ kg s}^{-1} \) \cite{10}, \( \eta = 2.07 \), \( \Delta d = 0.31 \mu \text{m} \), \( N = 158 \), \( T = 75 \text{ m} \), \( \varepsilon_0 = 8.854 \times 10^{-12} \) and \( x_0 = 5 \mu \text{m} \).

Note that the value of the lateral damping \( \frac{d_0}{(L_A)^2} \) cannot be accurately determined using either analytical approach or FEA. However, it can be roughly estimated from the ratio of squeeze damping and Couette damping \((12\), p 328, 337\). This ratio is given as

\[
\left( \frac{d_0}{L_A} \right) / d_x = \left( \frac{96 \xi LT^3}{\pi^4 d^5} \right) / \left( \frac{\xi LT}{d} \right)^2 \approx \left( \frac{T}{d} \right)^2.
\]

where \( \xi \) is the viscosity of air. For the comb drive given in this paper, the ratio (23) is approximately 900. Parameter \( d_0 \) is then easily calculated from \( d_x \) and \( L_A \) to be \( 5 \times 10^{-9} \text{ kg m}^2 \text{s}^{-1} \).

The simulation results illustrating the dynamic behavior of the system at the edge of the lateral instability are shown in figure 10. Note that for transient conditions, the pull-in voltage may be slightly different than for steady-state conditions.

4. Extended model for lateral actuator/sensor

With the model of the actual device developed in section 3, the additional features, intended for lateral sensing and actuation, can be added to the device, as shown in figure 2(b). For the lateral control analysis, these features are assumed not to have mass and damping. The detailed structure of both the lateral actuator and the sensor are shown in figure 11.

The lateral actuators contain top and bottom comb drive structures designed to generate force in the \( y \)-direction. These comb drive structures are unbalanced with different gaps (\( a \) and \( b \)) between the electrodes, as shown enlarged in figure 11. The maximum generated force occurs when the ratio of the smaller and the larger electrode gaps is \( a/b = 0.42 \) \cite{11}. Hence, the smaller gaps are defined by the minimum processing geometry; i.e., \( a = 2.5 \mu \text{m} \). The larger gap is 6 \( \mu \text{m} \) wide.

The lateral sensor has a similar gap geometry in order to achieve maximum sensitivity. The number of fingers, \( N_s \), and the initial electrode overlapping, \( \lambda_{s0} \), may vary. Movable capacitors are connected to the bridge structure through serial capacitors \( C_s \). Deflection in the \( y \)-direction can be determined from the difference between voltages \( V_{ST} \) and \( V_{ST} \). Note that, due to the bridge structure of the sensor, any out-of-plane motion affects both sensing voltages equally, thereby canceling its influence with respect to lateral sensing. The structure of both the actuator and sensor ensures that no force is generated in the \( x \)-direction.

The capacitance of the two parallel top actuating capacitors, \( C_{TP} \), with respect to the \( x \)- and \( y \)-directions is given as

\[
C_{TP}(x, y) = 2\varepsilon_0 T N_s x_{a0} \left( \frac{1}{a - y} + \frac{1}{b + y} \right).
\]
Similarly, the two parallel bottom capacitances, $C_{BT}$, are given as

$$C_{BT}(x, y) = 2\varepsilon_0 T N_a x_0 \left( \frac{1}{a + y} + \frac{1}{b - y} \right).$$

Both capacitances in (24) and (25) do not depend on $x$. Consequently, their contribution to the force along the $x$-axis does not exist. The unbalance coefficient $\Delta d$ is omitted in (24) and (25), because it is assumed that the sensing voltage is too low to influence lateral instability, and the actuation voltage is assumed to be an issue for the controller.

The total force in the lateral direction, $F_{ya}$, is given as

$$F_{ya} = \frac{1}{2} \frac{\partial C_{TP}}{\partial y} V_{aT}^2 + \frac{1}{2} \frac{\partial C_{BT}}{\partial y} V_{aB}^2$$

and, after some algebraic manipulation:

$$F_{ya} = \varepsilon_0 T N_a x_0 \left[ \left( \frac{1}{(a - y)^2} - \frac{1}{(b - y)^2} \right) V_{aT}^2 + \left( \frac{1}{(b - y)^2} - \frac{1}{(a - y)^2} \right) V_{aB}^2 \right].$$

Following a similar procedure, the force generated by the sensor is given as

$$F_{is} = \varepsilon_0 T N_a x_0 \left[ \left( \frac{1}{(a - y)^2} - \frac{1}{(b + y)^2} \right) V_{aT}^2 + \left( \frac{1}{(b + y)^2} - \frac{1}{(a - y)^2} \right) V_{aB}^2 \right].$$

Now, the new forces in (27) and (28) are integrated into the model in (20) as

$$J_s \ddot{y} + \frac{d_0}{L_A} \ddot{y} + \frac{k_y}{L_A} y = -\eta F_y + \eta \frac{L_a}{L_A} F_{ya} + \eta \frac{L_s}{L_A} F_{is}.$$

Additional parameters, necessary to conduct simulations and design the controller, are: $a = 2.5 \mu m$, $b = 6 \mu m$, $N_A = N_s = 60$, $L_a = 246 \mu m$, $L_s = 400 \mu m$ and $x_{A0} = x_{S0} = 20 \mu m$.

5. Control system design

The purpose of the paper is to describe a feedback approach that prevents lateral pull-in and extends the working range of the comb drive actuator in the primary $x$-direction DOF. The primary requirement for the controller is to keep $y = 0$. Additional shaping of the dynamics is also desirable. The detailed design of the controller is avoided due to available space, but the structure, parameters and operational description are discussed.

The structure of the controller is shown in figure 12. In order to simplify the analysis, a linear PID controller is implemented for the lateral DOF. We assume that the deflection $y$ is measurable and available. When the lateral feedback loop is closed, the sensed value of $y$ is compared to the referent $y = 0$, and the error signal is then passed through the controller. Saturation-type non-linearities distribute voltages to the two channels leading to the left and right $y$-direction comb drive electrodes. The signal is then taken through the square root functions that take care of the electrostatic force being dependent on the squared value of the voltage (11).
6. Conclusions

The novel approach to counteract the lateral instability of the electrostatic comb drive actuator was presented. The existing comb drive with its well-developed, experimentally verified mathematical model in one degree-of-freedom (DOF) [10] was extended with the lateral DOF model. The parameters of the lateral DOF model were determined through finite element analysis (FEA) and verified by static experimental results. This model was hypothetically extended with both sensor and actuator functions for lateral movement. Additional features were used to design the controller for lateral motion. Observations of the simulation results accomplished for the control system motivated several important conclusions.

The introduction of lateral feedback extends the range of the electrostatic comb drive actuator. The amount of extension depends on how much force is generated with the lateral actuators. This approach can be easily combined with conventionally used suspension design methods extending the design possibilities.

Unfortunately, the utilization of feedback requires lateral sensor and actuator functionalities which have to be added to the MEMS device. These features can significantly change the inertia and, consequently, the dynamics of the device. Additional processing electronics for sensing and control should be integrated with MEMS device as well. The sensing realization is not a problem as reliable techniques are available for high-resolution capacitance measurements [15, 16]. Moreover, the lateral controller should be kept as simple as possible in order to prevent unnecessary consumption of valuable space.

Despite the mentioned disadvantages, the initial results are promising and may pave the way for the more extensive use of feedback-based counteracting of lateral instability in practice.

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References

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