Department of Electrical Engineering

Interruption of a dry-type transformer in no-load by a vacuum circuit-breaker

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Abstract

This paper describes an investigation of the overvoltages generated during interruption of a dry-type delta-star connected transformer in no-load by a vacuum breaker. Attention is paid to the experimental set-up and the data acquisition system. Only moderate overvoltages were generated during interruption of the steady-state no-load current. During interruption of inrush current 37% of the phase-to-ground overvoltages were higher than 5 p.u. and 6% higher than 7 p.u. Results are compared with Boyle's calculation model and the experimental results obtained by Ohashi et al.

Comparison of experimental and theoretical results using Boyle's model showed no discrepancy for the inrush currents and the clean overvoltages from the steady-state interruption. The calculated values were somewhat higher than the experimental results. Overvoltages due to repetitive reignitions are not covered by Boyle's model. They were higher than the calculated values during steady-state switching. Magnetic and electrostatic remanence were largely excluded during the experiments but might influence the results in practice.

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1. Introduction.

During interruption of a small inductive current by a circuit breaker the arc always chops before the natural current zero. The magnetic energy stored in the inductance is then transferred into the circuit capacitances and vice versa. The result is a damped oscillation in the TRV \(^*\) of the switched-off circuit. The occurring phenomena with their adequate terms are summarized in fig.1.1. A more extensive treatment is given by Berneryd et al. (1981).

The maximum energy which can be transferred into the capacitance is

\[
\frac{1}{2} C U_m^2 = \frac{1}{2} L I_0^2 + \frac{1}{2} C U_o^2 \quad (1)
\]

with
- \(L\) = inductive load to be switched off
- \(C\) = capacitance in parallel with \(L\)
- \(I_o\) = chopped current through \(L\)
- \(U_o\) = voltage across \(C\) at the moment of current chopping
- \(U_m\) = maximum voltage across \(C\)

If damping is neglected the maximum voltage (\(=\) recovery peak) may be found from eq. (1):

\[
U_m = \sqrt{U_o^2 + (I_o \omega_t L)^2} \quad (2)
\]

where

\[
\omega_t = (LC)^{-\frac{1}{2}} \quad (3)
\]

is the frequency of the TRV.

A large inductance \(L\), a small capacitance \(C\) and, above all, a high chopping current \(I_o\) may be reasons for unacceptable high overvoltages as can be seen from eq. (2).

In practical power system networks the phenomena are usually much more complicated than described above. This may be due to the following reasons:

- The capacitances and inductances involved are distributed elements of the inductor windings and connections;
- Damping elements are involved; their influence may even be dominating in case of a transformer in no-load;

\(^*\) TRV = Transient recovery voltage. (In IEC 56-1 this abbreviation is restricted to the transient recovery voltage across the circuit breaker terminals. In this paper the term is used in a more general sense).
Fig. 1.1. Survey of chopping phenomena in a single phase circuit.
1: current to interrupt, $i_a$
2: voltage across circuit breaker (c.b.)
3: voltage across inductive load ($L_t$)
4: failed interruption due to short contact distance
5: influence of arc collapse
6, 7: instability oscillation leading to current chopping
8: chopping level
9: main voltage
10, 11: first maximum across c.b., ind. load, "suppression peak"
12, 13: second maximum across c.b., ind. load, "recovery peak".
- Many elements are frequency dependant;
- All circuits are three-phase networks; the interruption in the three phases succeeds after 60 or 90 degrees (depending on whether the neutral is grounded or not);
- The three phases often are inductively or capacitively coupled. As a result the transients penetrate from one circuit into the others, often as multi-frequency oscillations;
- Through these couplings even "virtual current chopping" is possible (Murano et al., 1974 a, Panek and Fehrle 1975);
- Steeply rising and/or high recovery voltages may cause reignitions in the circuit breaker which abruptly change the transient conditions;
- Repetitive reignitions shortly after current zero may cause "voltage escalation" (Murano et al., 1974 b). This phenomenon is especially prominent after virtual current chopping.

A detailed treatment of these phenomena in three-phase linear reactor circuits is given by Van den Heuvel and Papadias (1983). The interruption of the large, non-linear inductances of transformers in no-load will usually cause less concern. The overvoltages are not extremely high. Reasons for this are:
- The amplitude of the steady-state no-load current is usually much lower than the average chopping current. This means that in eq.(1) and (2) the value of $I_o$ is not determined by the chopping tendency of the breaker;
- The iron losses represent a large damping of the theoretical maximum of eq.(2). Often the TRV degenerates into a single strongly damped voltage pulse.

However, not every interruption of a transformer current is without any risk:
- Some combinations of c.b.'s and transformers allow chopping of relatively high currents, followed by one or more reignitions, especially when the contact gap is still small. A reignition around the recovery peak ("13" in fig.1.1) may give rise to a HF-oscillation superimposed on the main voltage ("second parallel oscillation"). Without any damping a maximum overvoltage of $(k_a + 2)p.u.$ could be attained as indicated in fig.1.2. ($k_a = \text{overvoltage in p.u., } 1 \text{ p.u. is the rated phase voltage}$).
Fig. 1.2 Overvoltages due to reignition.

- Much more involved is the interruption of a transformer inrush current. The occurring peak currents may be as high as 8 times the amplitude of the rated current and thus up to several hundred times the peak value of the steady state no-load current. Moreover the inrush effect damps out slowly and may be active for a minute or more, see fig. 1.3.

Fig. 1.3. An example of inrush-currents in two phases of a dry-type transformer (1600 kVA).
If this current is interrupted it is very well possible that \( I_o \) is determined by the c.b. However, it should be stated that interruption during inrush is not a common procedure if the transformer protection is reliable.

In the last decade the application of both dry-type transformers and vacuum c.b.'s in MV-networks progressed strongly and a combination of these two apparatus is now common. There is an essential difference between vacuum-breakers and the more "classical" c.b.'s with respect to the origin of current chopping. The arc in a vacuum-breaker is sustained in a high temperature metal vapour which is released from the cathode. The total arc voltage for small currents is of the order of 20 V and only a fraction of this voltage is effective between the cathode region and the anode. Therefore, current chopping is a cathode effect and the chopping level is in first approach determined by the choice of the contact material. The origin for current chopping in other c.b.'s is instability of the arc column. This instability and consequently the chopping level is strongly dependant on the circuit-elements around the breaker. Especially, the capacitance \( C_p \) seen in parallel with the breaker has a dominating influence on the chopping level. In first approach this can be expressed by (Berneryd et al, 1981)

\[
I_o = \kappa \sqrt{C_p}
\]  

(4)

Here \( \kappa \) is a "chopping number" which order of magnitude is \( 5 - 15 \times 10^4 \, (AF^{-2}) \). \( \kappa \) is more or less constant for a specific circuit breaker.

In practical MV-networks \( C_p \) nearly equals the capacitance \( C \) of the transformer and the connecting cables on the load side. If eqs. (3) and (4) are introduced in (2) one finds for the "classical" c.b.'s a maximum overvoltage:

\[
U_m = \sqrt{U_o^2 + \kappa^2 L}
\]  

(5)

and for a vacuum breaker:

\[
U_m = \sqrt{U_o^2 + I_o^2 L/C}
\]  

(6)

This shows that a small capacitance on the load side may give rise to a high overvoltage when a vacuum-breaker is used.
Dry-type transformers have small inherent capacitances. This is one reason for investigating the combination of a dry-type transformer in no-load and a vacuum c.b., especially when interrupting inrush currents. Other reasons are:
- Above all vacuum-breakers are able to interrupt steeply falling currents. Therefore, they may cause virtual current chopping and voltage escalation in some cases;
- Dry-type transformers often have a lower surge withstand strength than oil immersed transformers.

This paper describes such an investigation. Ohashi et al. (1976) have also treated this subject. The results of both studies are compared.

The non-linearity of the iron circuit prohibits an exact calculation of the overvoltages. During the last years several approximative methods were published which, according to their authors, produce satisfactory results (Boyle 1982, Tuohy and Panek 1978, Ihara, Panek and Tuohy 1983). In this paper Boyle’s method is briefly summarized and compared with our experimental results.

2. Experimental procedure

Two series of measurements were performed. In the first series the steady-state no-load current was interrupted; in the second series interruption of the inrush-current occurred approximately 160 msec after the onset of current flow. The circuit used is shown in fig. 2.1. In Annex 1 data of the apparatus used are collected.

Fig. 2.1. The circuit used for the experiments.
The voltage transients were measured in three phases using mixed voltage dividers. Current was measured in the U-phase only in the first series and in three phases in the second series. In the latter case a Pearson current-transformer was used in the W-phase. Due to the high level of the dc-component in the inrush current the transformer core was usually driven into saturation and as a result a number of these current data were not reliable. The other currents were measured by low-inductance coaxial shunts specially constructed for these types of measurements. Some particulars of the shunts will be given below.

2.1. The high voltage coaxial shunt

The two shunts used are almost identical in design and incorporate the following features (see also fig. 2.2):
- current measurement at high-voltage level is achieved by a fibre optics data transmission system;
- the shunt design is such that both the resistor element as current sensor and the data transmission system are effectively shielded from outer disturbances;
- the transmitter is positioned in a field-free region with respect to the field generated by the current the shunt carries;
- the resistance elements can easily be exchanged which provide for an extended range of current measurement.

![Fig. 2.2. The high-voltage coaxial shunt.](image-url)
The resistive part of the shunt consists of four morganite resistors in parallel having a total resistance of 0.16Ω. Due to their limited length and relatively large diameter their inherent inductance is kept at a minimum. The resistors are pressed between two aluminium flanges which combine shielding and cooling purposes. The voltage difference over the flanges is fed into the transmitter (Develco INC, model 6113) positioned in an aluminium cylinder. Additional shielding is obtained by a mu-metal foil. The output signal is transmitted by fibre optics to a receiver (Develco INC, model 6123) located together with the data-acquisition system in a Faraday cage.

2.2. The data acquisition system

The system.

A system built in compliance with the CAMAC standard (Computer Aided Measurements and Control) is used for the storage and processing of data. Its core consists of a LSI-11 processor (Digital Equipment Corporation) combined with a dual-diskette drive as background memory (2 x 500 K Bytes), a memory of 32 K Bytes, 2 communication interfaces for the use of terminals and a CAMAC crate. The latter is a 19-inch rack incorporating a power supply, a control unit and 23 stations for in- and output units. The control unit governs the data transmission between these units and the computer. For the implementation of the unit functions a Fortran programme with special routines (FORTRAN-CAMAC) is used.

Available CAMAC-units are:
- 2 waveform digitizers, maximum sample frequency of 20 MHz, 1 K Byte buffer memory;
- 1 8-channel waveform digitizer, sample frequency 500 kHz, 32 K Byte buffer memory;
- programmable clock for external control of the waveform digitizers, frequency 20 Hz - 20 MHz;
- general purpose interface 16 bit in- and output register;
- 2 channel DAC for the analogue output to an XY-recorder.

The entire system is positioned in a Faraday cage.

The data acquisition.

The measured analogue signals are first converted to a maximum level of 0.5 V with respect to the grounded cage. By the AD converters of the waveform digitizers the signals are sampled (8 bits level) and digitally stored during the measurement in the buffer memories.
In case of the 8-channel digitizer a choice in the registration of 1, 2, 4 or 8 different signals is possible. When using more than one channel the channel entries are multiplexed with a frequency of 5 MHz. Dependant upon the number of channels used the maximum sample frequency and the maximum amount of samples per channel vary according to the values shown in the following table:

<table>
<thead>
<tr>
<th>No. of channels</th>
<th>Maximum sample frequency</th>
<th>Maximum no. of samples for each channel</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>0.5 MHz</td>
<td>4 K</td>
</tr>
<tr>
<td>4</td>
<td>1 MHz</td>
<td>8 K</td>
</tr>
<tr>
<td>2</td>
<td>2 MHz</td>
<td>16 K</td>
</tr>
<tr>
<td>1</td>
<td>4 MHz</td>
<td>32 K</td>
</tr>
</tbody>
</table>

The sampling rate of the digitizers is controlled by an internal clock; in case of the single channel digitizers the sampling rate can be varied between 40 kHz - 4 MHz. An extended sampling rate is possible by the external clock which is programmable between 20 Hz and 20 MHz in 1-2-5 sequence. Another feature of the extended clock is importance sampling at (a maximum of) three different frequencies. Using the DA converters of the digitizers, the contents of the buffer memory can be displayed directly on a monitor. As an alternative and using the LSI-11 processor the data can be plotted on an XY-recorder.

After each measurement the contents of each buffer memory must be copied. This is done by the LSI-processor which copies the data in a file on diskette. In this way further processing of the data can be executed, for which another computer may be used. Programs are available for the calculation of the maxima and minima of the signals. This is used in plotting the signals on a grid together with their axes and scale divisions. Also programs for processing the data on this system have been developed.

The sequence generator.

A microprocessor is used as a programming device for the apparatus employed in the experimental circuits. To this purpose the system design kit SDK-85 (INTEL) was extended by the following components:

- a zero crossing detector. At each zero crossing of a reference signal the detector will supply a pulse to an interrupt input of the processor.
Synchronizing of a programme cycle with the frequency of the mains voltage is thus achieved as the programme will start with reference to a specified zero crossing;

- output buffers and drivers.

22 out of 76 available output ports are supplied with buffers. These are connected with 22 output drivers by opto-couplers for galvanic separation. Each driver supplies 50 mA at 25 V.

The programme for the microprocessor is stored in part in an EPROM (Erasable Programmable Read Only Memory) and in part in a RAM (Random Access Memory). In the latter the relevant data for the delay-units are stored in groups of 5 instructions each.

Six types of delay-units are available:

1) type A : steps of 1 sec each
2) type B : steps of 1 millisec each
3) type C : steps of 1 millisec each, followed by a pulse lasting 1 millisec.
4) type D : steps of 8 microsec each
5) type E : steps of 8 microsec, starts after the first following zero crossing
6) type F : as E, followed by a pulse lasting 1 millisec.

The programme first decides the type of delay-unit involved, as a next step the proper subroutine is executed for this delay and finally the appropriate port number output is changed.

3. Experimental results

3.1. Interruption of the steady-state no-load current

After decay of the transient stage the no-load current of the transformer attains a stationary peak value of 0.5 A. The vacuum breaker investigated has an average chopping level of several amps and as a result the current is chopped immediately after contact separation in any phase.

In 44 3-phase interruptions the moment of contact separation was spread uniformly over a period of more than 10 msec; this means that the chopping level has varied from 0.5 A down to zero. Oscillograms which are typical for these interruptions are shown in fig. 3.1 and 3.2.

During interruption a number of reignitions may appear due to the initial small value of the gap size. Fig. 3.3a and b show the frequency distributions of the maximum overvoltages for this series.
Fig. 3.1 and 3.2. Oscillograms of transient voltages during interruption of the steady-state no-load current.
Fig. 3.3a and b. Frequency distributions of the maximum overvoltage in each phase, \( N = 132 \) for each distribution. Type A overvoltage: fig. 3.3a, type B overvoltage fig. 3.3b.

A distinction has been made between overvoltages during (repetitive) reignitions, type A, and overvoltages without a reignition, type B. In first instance the result of a reignition is the limitation of the prospective overvoltage. However, repetitive reignitions may cause voltage escalation resulting in extra high overvoltages with an extremely steep rate of rise. Type B overvoltages may either be "clean" transients (without any reignition) or the last surge peak after repetitive reignition. (See for illustration figs. 3.1 and 3.2).

A maximum value of \( k = 3.5 \) was observed, i.e. the overvoltages during interruption of the steady-state no-load current have moderate values. Higher overvoltages (\( > 2.25 \) p.u.) were always of the A-type and were caused by voltage escalation.

### 3.2. Interruption of the inrush-current

Protection of the transformer windings at a level of 55 KV was achieved by 3 voltage arrestors consisting of spark gaps each in series with 100\( \Omega \) and positioned between the high voltage side and the yoke of the transformer. A number of 40 3-phase interruptions were done. After each experiment the capacitors of the cable and the transformer casings were discharged.
In order to avoid a too strong interaction of subsequent interruptions, the transformer was demagnetized to a certain extent. This was done by switching-on the transformer after each test. Once the magnetizing current had reached its stationary value the transformer was switched-off again.

Fig. 3.4 and 3.5 show examples of oscillograms. Frequently repeated reignitions are observed and steep voltage gradients are present (10 - 35 kV \( \mu \text{sec}^{-1} \)).

Fig. 3.6 shows the frequency distributions of the overvoltages. In 5 out of 40 tests the arrestors were activated; i.e. the transients were larger than 55 kV.

\[ K = \frac{\hat{U}}{U_f} \]

Fig. 3.6 Frequency distribution of the maximum overvoltage in each phase during interruption of inrush current \( N = 120 \).

The measurements show that interruption of the inrush current for this particular set up results in unacceptable high overvoltages (\( K \geq 7 \)).
Fig. 3.4 and 3.5. Examples of voltages and currents during interruption of the inrush current.
3.3. Accumulation of electrical charges.

During interruption of inrush-currents spark discharges along the epoxy-resin surfaces of the different phases or between the phases were repeatedly observed. After the customary procedure of discharging the circuit capacitances during each test charges were observed on the epoxy-surface particularly in the regions where the distance between the phases is a minimum. These charges remained present for hours. During interruption of the stationary no-load current these phenomena were much less outspoken.

The effect was particularly strong if no demagnetization and discharging of capacitors on the epoxy-surface occurred. In one case the inrush-current was interrupted twice without an intermediate demagnetization or discharging procedure. Fig. 3.7 shows the (strong) light emission occurring during the second interruption.

Fig. 3.7. Light phenomena during interruption of inrush current.
Discharges are visible along and between the surfaces of the V and W phases. (The arrestors were not activated during these two tests). Inspection of the epoxy-surfaces after the inrush-current test revealed no visible damage (It is very likely that the energy-content of the process is very low).

The presence of surface charges may influence the shape and magnitude of the overvoltages generated during interruption. Whether the overvoltage factor is influenced in a positive or negative sense could not be established from these measurements, though in one or two oscillograms the transients showed a flat maximum which may be due to local discharging of the epoxy-resin.

4. Boyle's calculation model.

This is a simple empirical model. It is based on the well-known concept of "magnetic efficiency" \( \eta \) (also called "efficiency of release of magnetic energy") for a single phase transformer.

The definition of \( \eta \) is related to the B-H-loop of the transformer (see fig. 4.1) by

\[
\eta = \frac{W(\omega)}{W_{id}} = \frac{\text{Area } P}{\text{Area } (P + Q)}
\]

with

\( W(\omega) = \) released magnetic energy from the transformer after magnetizing up to the point \( I_m, B_m \)

\( W_{id} = \) stored magnetic energy in an ideal linear inductor when \( i_m = I_m \) and \( B = B_m \)

\( i_m = \) magnetizing current

\( B = \) flux density, so

\[
W_{id} = \frac{1}{2} N A B_m I_m', \text{ where}
\]

\( N = \) number of windings

\( A = \) cross section of the iron core

\( \omega = 2\pi . \text{frequency of demagnetizing} \)

The released magnetic energy \( W(\omega) \) during steady state operation may be determined from the transformer magnetizing loops \( B(i_m) \). It has a maximum for \( \omega \to 0 \).
Due to eddy losses the $B(i_m)$-loops grow wider with increasing frequency. Therefore, $\eta$ is decreasing with frequency and also depending on $B_m$.

After chopping a steady state magnetizing current $I_o = I_m$, this current and the flux density will decay following a line as indicated in fig. 4.2 by $b(\omega)$.

During this decay the induced voltage is

$$u = NA \frac{dB}{dt} = (NA \frac{dB}{di_m}) \frac{di_m}{dt} = -\int \frac{i_m}{C} dt$$

So the TRV-current is given by

$$i_m + NAC \frac{dB}{di_m} = \frac{d^2i_m}{dt^2} = 0$$

This is not a (damped) sine-curve because $dB/di_m$ is not a constant. Actually the transient oscillation has no defined frequency. This complicates the evaluation of $\eta$.

Boyle developed a practical way out in two steps:

a. He calculated $\eta$ as a function of $B_m$ from steady state $B(i_m)$-loops for a number of frequencies, both for CROS-cores (fig. 4.3 and for HRS-cores. (CROS = cold reduced oriented steel, HRS = hot rolled steel, an older core material, used prior to 1960).
b. He defined an "effective inductance" in order to estimate an "effective frequency":

\[ L_{\text{eff}} = NA \left( \frac{\partial B}{\partial i_m} \right) (i_m = I_m) \]  \hspace{1cm} (11)

\[ f_{\text{eff}} = \frac{1}{2\pi L_{\text{eff}} C} \]  \hspace{1cm} (12)

This effective frequency is used to obtain \( \eta \) from fig. 4.3. The generated overvoltage may then be calculated from

\[ \frac{1}{2} C U_m^2 = \frac{1}{2} \eta NA B_m I_m + C U_o^2 \]  \hspace{1cm} (13)

(compare with eq. (1)).

In eq. (11) \( NA \) may be found from the steady-state voltage:

\[ \hat{U} \sin \omega t = NA \frac{\partial B}{\partial t} \]  \hspace{1cm} (14)

So \( NA = \hat{U}(\omega B_{\text{max}})^{-1} \)  \hspace{1cm} (15)
where

\[ U = \text{amplitude of the rated voltage} \]
\[ B_{\text{max}} = \text{maximum flux density at rated voltage} \]
\[ \omega_I = \text{main frequency} \]

Boyle further assumed in eq. (11):

\[ \left( \frac{\partial B}{\partial i_m} \right)_{i_m = I_m} = \frac{0.15 B_m}{I_m} \]  \hspace{1cm} (16)

So from eq. (13) \( U_m \) can be calculated provided the \( B(i_m) \)-loop of the transformer and the chopping current are known.

As stated before, in most MV and HV transformers the magnetizing current is lower than the possible chopping current. Then the most severe over-voltage occurs when the magnetizing current chops at its maximum:

\[ I_o = I_{\text{max}}; \quad B_m = B_{\text{max}}; \quad U_o = 0 \]  \hspace{1cm} (for a single phase transformer).

Introducing this in eqs. (11) and (13) and using eqs. (15) and (16) two simple expressions result:

\[ L_{\text{eff}} = \frac{0.15 \hat{U}}{\omega_I I_{\text{max}}} \]  \hspace{1cm} (17)

\[ U_{\text{max}} = \sqrt{\frac{\eta 0}{\omega_I C} I_{\text{max}}} \]  \hspace{1cm} (18)

Calculations for the inrush condition are performed on the same basis as for the steady-state current. The flux density at the point of current interruption may then be estimated from fig. 4.4 (for CROS cores) where 1 p.u. is \( I_m \) for \( B_m = 1.7 \text{ T} \).
The effective frequency to assess \( \eta \) is derived "from the slope of the line on the transformer magnetization curve at the point of interruption and the point cutting the zero current line at a flux density of 1.75 T" (Boyle, 1982). An example is indicated in fig. 4.4 for \( I_o = 20 \text{ p.u.} \), e.g. for \( I_{\text{max}} = 0.5 \text{ A}, B_{\text{max}} = 1.7 \text{ T} \) at steady state operation and \( I_o = 10 \text{ A} \) during interruption of inrush current.

In this case

\[
L_{\text{eff}} = \frac{(2.0 - 1.75)}{10} \cdot \frac{\bar{U}}{w_1 B_{\text{max}}} \quad (19)
\]

With \( \bar{U} = 10\sqrt{2}/\sqrt{3} \text{ kV} \) and \( w_1 = 314 \): \( L_{\text{eff}} = 0.40 \text{ H.} \)

Compare with \( L_{\text{eff}} \) in steady state condition: 7.8 H.

Boyle's calculation model is essentially a single phase model. However, the author also applies his method for the calculation of overvoltages in three phase transformer switching. In doing so he makes no distinction between Y-d connected, D-y connected or Auto-transformers, nor in the way of grounding the neutral. The range of his MVA-ratings vary from 1 to 800 MVA.

5. Comparison of experimental results with Boyle's calculation method and Ohashi's results.

Using Boyle's method to calculate the possible overvoltages in a D-connected transformer in no-load leads to some problems:

- The chopped currents are line currents. They deviate from the currents in the transformer windings in a complicated way, whereas the winding currents are responsible for the overvoltages.

  This effect is even more pronounced during the inrush period.

  It is not clear, therefore, which current should be introduced in equations (17) and 18) and in figs. 4.3 and 4.4.

- In the interrupted circuit three phase-to-ground and three phase-to-phase capacitances are involved. It is not clear which part of them should be introduced for calculating the effective frequency, eq. (12), and the overvoltage, eq. (18), as well in first-pole-clearance as in second-and-third-pole-clearance.

- After interruption of the second and third pole a d.c. voltage will generally remain in the interrupted circuit, forming an extra contribution
to the overvoltage (Van den Heuvel 1981). This d.c. level is not taken into account in the calculations.

5.1. Exciting current interruption.

The maximum value of the line currents was 0.5 A which is significantly lower than the chopping level of the breaker. As a result the current chops at the very moment of contact separation and, therefore, more or less simultaneously in all three phases. The maximum value of 0.5 A was also taken as \( I_{\text{max}} \) in eqs. 17 and 18.

Using \( \tilde{U} = 10\sqrt{2}/\sqrt{3} \text{kV}, \omega_1 = 2\pi .50 \) one finds:

\[
L_{\text{eff}} = \frac{0.15 \tilde{U}}{\omega_1 I_{\text{max}}} = 7.8 \text{ H}.
\]

For the effective capacitance the average value of one phase to ground is chosen: 3.47 nF, see Annex 1. The effective frequency will then be:

\[
f_{\text{eff}} = \frac{1}{2\pi L_{\text{eff}} C} = 967 \text{ Hz}.
\]

From fig. 4.3 \( \eta = 0.1 \) may be obtained. Eq. (18) gives the maximum theoretical overvoltage:

\[
U_{\text{max}} = \sqrt{\eta \tilde{U} I_{\text{max}} / \omega_1 C} = 19.4 \text{ kV} = 2.4 \text{ p.u.}
\]

In our experiments a maximum type B overvoltage of 18.4 kV (2.25 p.u.) was measured (see fig. 3.3), which is in good agreement with the calculated value.

Some type A overvoltages with higher magnitudes (up to 28.4 kV, 3.5 p.u.) were measured. In all these cases voltage escalation occurred. This phenomenon is not covered by the (simple) calculation model.

5.2. Inrush current interruption.

Here the maximum value of the line currents at the moment of contact separation is some hundred amps., which is much higher than the chopping level of the breaker. Therefore, another procedure was followed here.

For each interruption the highest of the three chopping values was used to calculate the maximum overvoltage. This calculated value was then compared with the measured values in the three phases. The method will
be demonstrated by an example of a specific measurement (nr 72). The maximum chopping current was $I_0 = 3.26\, \text{A}$, which is 6.5 p.u. if compared to the no-load current. From fig. 4.4 a value $B_{\text{max}} = 1.9\, \text{T}$ is taken. Now

$$L_{\text{eff}} = \frac{1.9 - 1.75}{3.26} \cdot \frac{\hat{U}}{\omega_1 B_{\text{max}}} = 0.70\, \text{H},$$

$$f_{\text{eff}} = 3320\, \text{Hz} \quad \text{and} \quad \eta = 0.074$$

It can easily be shown that in eq. (13) the term $\frac{1}{2} \hat{U}_0^2$ is much smaller than the electromagnetic combination, even for $U_0 = \hat{U}$. Therefore, eq. (13) reduces to

$$U_m = \sqrt{\frac{\eta NAB_{\text{max}}}{C}} = \sqrt{\frac{\hat{U}_0 I_{\text{max}}}{\omega_1 C} \cdot \frac{B_{\text{max}}}{1.7}}$$

which gives a theoretical maximum $U_m = 44.9\, \text{kV}$ for this specific interruption. The highest measured value in this case was 13.3 kV. (The overvoltages were obviously reduced by repetitive reignitions).

Fig. 5.1 Comparison of measured and calculated overvoltages as a function of the chopping current during interruption of inrush current.
All experimental results are collected in fig. 5.1 and compared with the theoretical curve for maximum overvoltage after "clean" interruption. Because of the discretisation of the current values by the recorder an uncertainty of ± 1/2 digit has to be taken into account. Therefore, each experimental result is indicated by a horizontal bar which represents the overvoltage range for the total possible current region. In nearly all cases the complete region lies below the calculated maxima. Many of the experimental values are significantly lower due to (repetitive) reignitions without voltage escalation. Here it should be noted that the overvoltage protection was activated 5 times, limiting the overvoltage to 57 kV (7 p.u.) for $I_o = 8$ A. From fig. 5.1 it can be seen that for this chopping current a voltage of 73 kV (8.9 p.u.) was calculated. The probability of occurrence of the theoretical maxima seems very low. It is not possible to conclude from our experiments whether the calculated values are too pessimistic or not.

5.3. Comparison with Ohashi's results.

In Ohashi's experiments a dry-type transformer 22 kV/6.6 kV, 2000 kVA in D-d-connection was interrupted by a vacuum breaker. His experimental set-up differed from our circuit, not only by the secondary d-connection but also by the source side impedance. Our circuit was connected to a 250 MVA municipal 10 kV cable network. In Ohashi's circuit a 220 V/22 kV "testing transformer" was used as the power supply and three 0.1 μF capacitors were inserted to ground on the primary side of the vacuum circuit breaker to simulate the cables. No data are given for capacitances at the load side nor for the MVA rating of the supply side transformer.

The cumulative frequency of occurrence of Ohashi's overvoltages is compared with our results in fig. 5.2. The overvoltages in steady-state no-load current interruption (curves a, b, c) cover a somewhat larger range in our experiments (1-3.5 p.u. against 1-3 p.u.) but our average value was lower. During inrush current interruption our overvoltage limitation was adjusted at 7 p.u. against 4 p.u. in Ohashi's experiments. The overvoltages generated in our experiments are represented by two distribution curves (curve e and f).
For curve f the maximum overvoltage in three phases during each interruption was taken. Curve e consists of the maximum overvoltages generated in each phase. As shown, there is a significant difference between these two interpretations. From the experiments of Ohashi et al. (1976) it is not clear which choice has been made, but at any rate our experiments show the occurrence of a much higher overvoltage fraction than they have in their experiments.

![Cumulative frequency distribution of switching overvoltages](image)

Fig. 5.2. Cumulative frequency distribution of switching overvoltages for stationary (curves a, b, c) and inrush (curves d, e, f) conditions. a) This report, type B; b) this report, type A; c, d) Ohashi et al. (1976); e) maximum overvoltage in each phase during an experiment (N = 120); f) maximum overvoltage during an experiment (N = 40).

Comparison of results in a more sophisticated way is not possible because of lack of information about the Japanese experiments and the difference between the experimental set-ups. It is clear that the large short-circuit impedance of the feeding network and the damping of the secondary delta-connection prevent the occurrence of extremely high inrush currents (up to 1000 A in our case against approx. 33 A in Ohashi's experiments.)
6. Conclusions.

Interruption of the exciting current of a dry-type transformer by a vacuum breaker does not give rise to dangerous overvoltages. In our experiments the maximum overvoltage was 3.5 p.u. and only a fraction of 2% was > 3 p.u. from a total number of 132 registrations. The higher part of the overvoltages was due to repetitive reignitions and voltage escalation.

Interruption of the inrush current of such a transformer should not be recommended because high overvoltages are possible in that case. Out of 120 registrations a fraction of 37% was higher than 5 p.u. and the overvoltages were 5 times limited to 7 p.u. by operation of the overvoltage protection. If this type of switching cannot be avoided the use of surge arresters seems to be inevitable.

These results are in accordance with an earlier investigation by Ohashi et al., though the fraction of high voltages during inrush current interruption was substantial higher in our experiments.

Estimation of the maximum overvoltages with Boyle's calculation model provides safe results for inrush current switching and for the clean overvoltages in case of interruption of exciting currents. This method is not developed to calculate overvoltages after repetitive reignitions and voltage escalations and may therefore not satisfy under such conditions.

Especially after interruption of inrush currents substantial remanent magnetism may be left in the transformer core and electrical charges may remain on the surface of the epoxy coil casings. These effects were not treated in detail in this investigation.
Annex 1.

Basic data of apparatus used in transformer no-load interruption.

Transformer.
Dry-type, cast-resin insulation, air cooled.
Rated voltage : 10,250/400 V
Rated current : $I_{\text{nom}} = 90$ A
Rated power : 1600 kVA

Measured capacitance between the high-voltage windings and the transformer core, $C = 1.2 \text{ nF}$.
Maximum peak value of the inrush-current measured in the circuit as shown in fig. 2.1., $I_{\text{inrush, peak}} = \pm 1000$ A.

Cables.
Crosslinked polyethylene; $1 \times 95 \text{ mm}^2$
Length : U - phase : 11.1 meter
V - phase : 11.9 meter
W - phase : 11.5 meter

Capacitance measured between conductor and cable shield (100 Hz - 10 kHz): U - phase: 2.17 nF; V - phase: 2.38 nF; W - phase: 2.28 nF.

Vacuum-breaker.
Copper electrodes with an external magnetic field.
Rated voltage : 12 kV
Rated current : 630 A
Symmetrical short circuit current : 20 kA

**Mixed voltage dividers.**

<table>
<thead>
<tr>
<th></th>
<th>Attenuation ratio</th>
<th>Primary resistance</th>
<th>Primary capacitance</th>
<th>Bandwidth</th>
</tr>
</thead>
<tbody>
<tr>
<td>U - phase</td>
<td>400:1</td>
<td>400 $\Omega$</td>
<td>25 pF</td>
<td>DC - 20 MHz</td>
</tr>
<tr>
<td>V - phase</td>
<td>600:1</td>
<td>600 $\Omega$</td>
<td>15 pF</td>
<td>DC - 20 MHz</td>
</tr>
<tr>
<td>W - phase</td>
<td>1000:1</td>
<td>100 $\Omega$</td>
<td>3 pF</td>
<td>DC - 75 MHz</td>
</tr>
</tbody>
</table>
Buffers used with the voltage divider.

Maximum input voltage 0.5 V - 200 V (peak values),
adjustable in 1 - 2 - 5 sequence.

Output: max. 0.5 V peak
Slew rate: 1 kV (μsec)^{-1}

Bandwidth: DC - 20 MHz
Input impedance: 1 MΩ // 20 pF

Coaxial shunt and optical transmission system.
Sensitivity range: 1 mV/7 mA - 1 MV/2.8 A
Bandwidth: DC - 30 MHz
Maximum pulse current: 1200 A peak

Pearson current transformer.
Type 301 A
Output: 10 mV/A
max. peak current: 50 kA
max. RMS current: 400 A
IT - max.: 22 Asec
Useable risetime: 200 nsec
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